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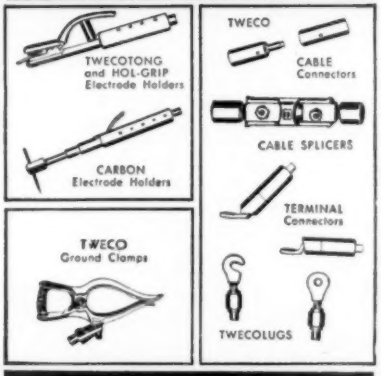

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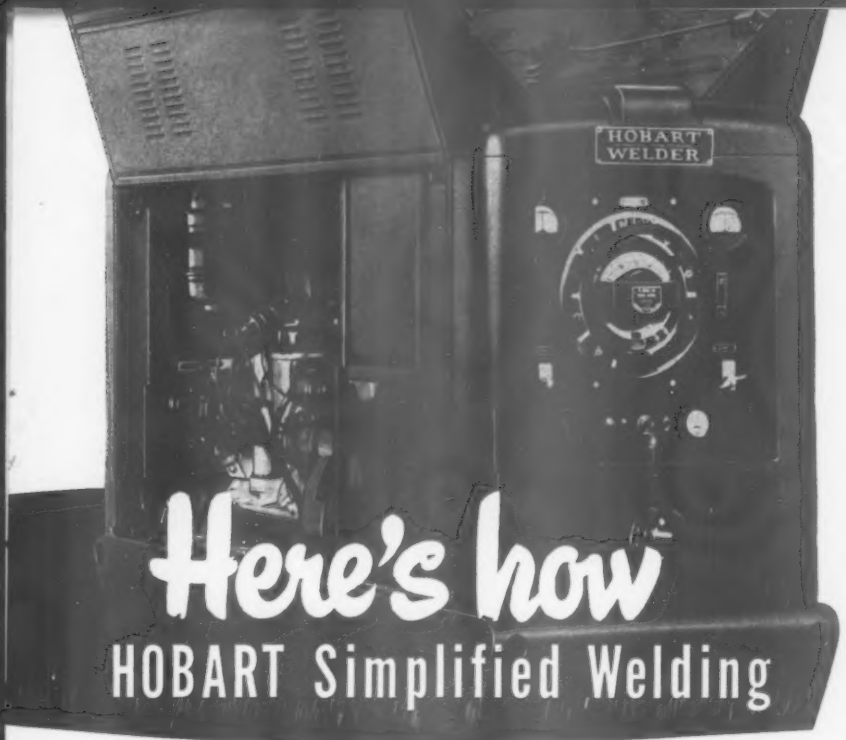

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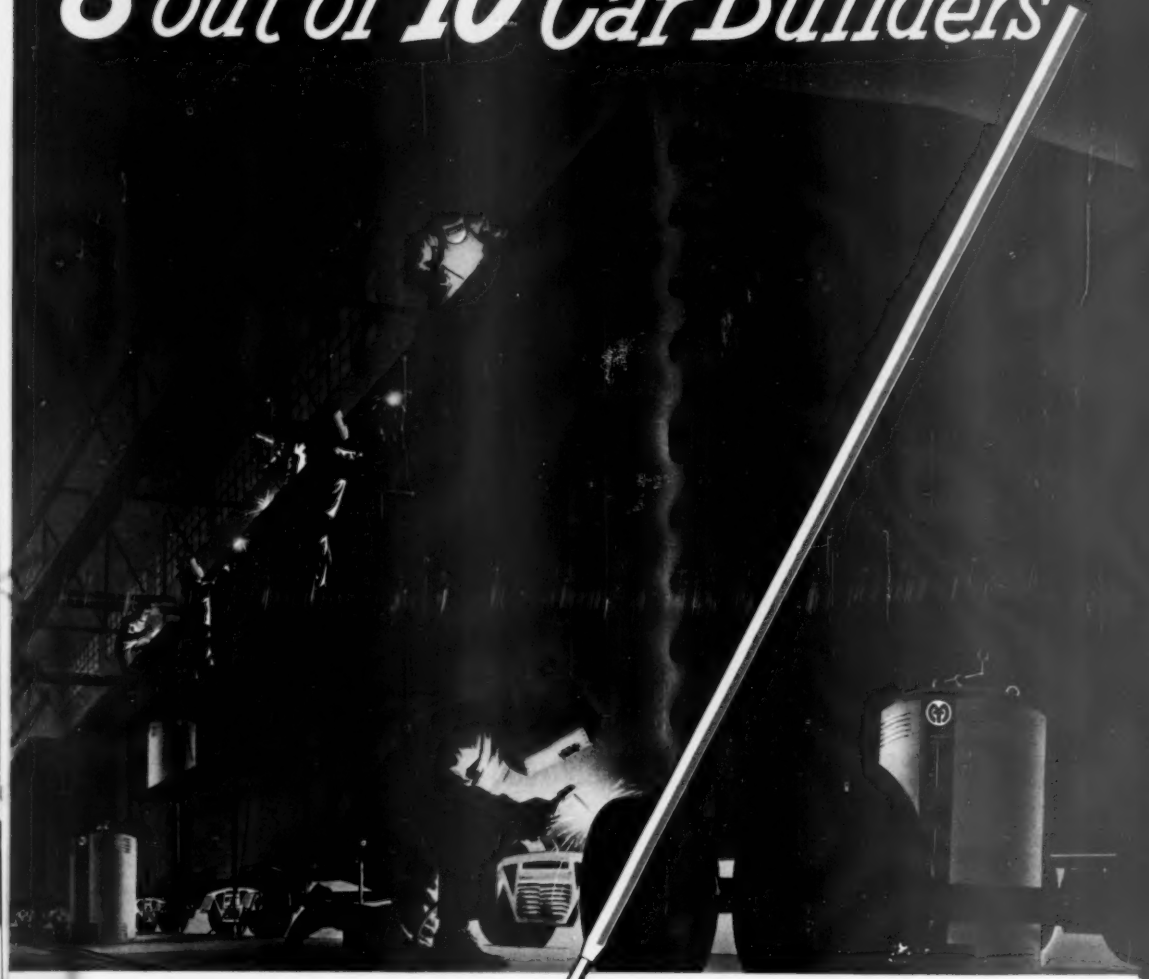

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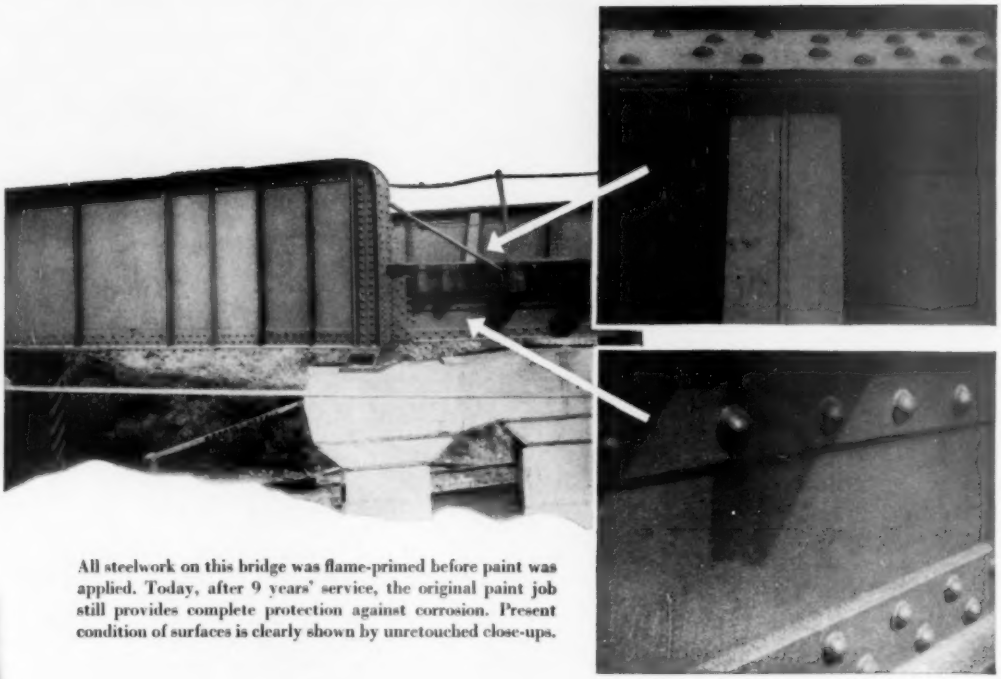

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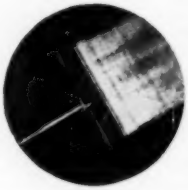

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The WELDING JOURNAL

volume 30 September 1951 number 9

Seam Welding Containers Automatically

Design of an automatic body-welding unit for seam welding to meet current manufacturing demands. Need of precautions is emphasized

by C. S. Seltzer

INTRODUCTION

THE economic advantage of line production as compared to manual manufacturing is generally well known and needs no supporting data here. In the automatic processing of lap-welded steel container bodies there are many preparatory conditions (not so generally known) that must be satisfied to produce a uniform and satisfactory gas tight package at a comparatively high production speed and minimum direct labor costs.

In this survey, we shall develop the design of an automatic body-welding unit that will meet the current manufacturing demands. Comparisons with manual methods are made to emphasize certain precautions arising because of the absence of machine operators. In the description of the processing operations, the work flow through an actual automatic line is followed. Comparisons with earlier seam-welding methods are made to indicate a design trend.

Lap control is recognized as a critical condition of consistently satisfactory automatic pail production. This is, therefore, developed in somewhat greater detail than the description of other functions. Thermal lap spread at the welding is analyzed and its influence upon the lap width is described. The influence of high-speed automatic performance at the welding operations is also developed.

It should be remembered that welding containers with automatic equipment demands greater accuracy and uniformity in processing than is required with manually operated equipment. The fundamental difference between the two manufacturing methods is the absence of the visual work inspection incident to the operator's task. Moreover, such items as the duty cycle, increased production, feeding the work to and through the machines, control of the work radially and longitudinally, become more critical functions in automatic equipment.

Although the subject matter reference is metal containers, it also applies to all cylindrical work of this character.

In the Preparation of Sheets, the stock welded in this investigation is between No. 18 to No. 28 gage mild steel. Lighter gages will be considered later. Slitters, shearing or blanking presses are usually employed for sheet sizing. Although slitters are the general preference, the greater accuracy required in sheet sizing and squaring, and such irregularities as wire edges and overlaps caused by dull cutters and incorrect setting are more critical in automatic processing. The tin container producer employs double slitters exclusively for this operation, but he periodically checks the work for irregularities. We suggest, when slitter sizing, this practice be followed in the heavier gage work.

Blank press sizing assures more consistent results but sacrifices production. The combination of slitting and shearing finds very little acceptance in automatic line operation because of production and other limitations.

In lap cleaning some producers prefer to increase their material cost differential and omit lap cleaning. This offers a reasonable assurance of consistent results, but only if visual inspection is employed. It should be considered that stock remaining in storage is exposed to atmospheric conditions and although the stack, as such, may offer some protection to the inner sheets, the exposed sheets may cause sufficient difficulty at welding and other operations to make this a questionable means of economy. In automatic production, precleaning the fay metal is virtually imperative. There are two well-proved methods of lap cleaning, viz., grinding and abrasive blasting.

C. S. Seltzer is connected with the Swift Electric Welder Co.

Scheduled for presentation at the Thirty-Second Annual Meeting, A.W.S., Detroit, Mich., week of Oct. 15, 1951.

The lap grinder produces a pattern on the top and bottom lap surfaces of each girth edge of approximately 0.002 to 0.005 x 3/8 in. wide, or to suit requirements. In this machine it is important that the leading corners of the work are not upset or bent out of position if jamming in passing through the grinding wheels is to be avoided.

The abrasive-lap-cleaning method replaces the wheels with blasting nozzles. These nozzles impinge a blast of fine abrasive powder on the marginal surfaces and produce a very satisfactory pattern. The abrasive is recovered, cleaned and later recirculated through the system. This apparatus has an additional advantage of by-passing defective leading corners.

SHEET FEEDING

There are at least three well-proved methods of transporting the prepared sheet to the receiving machine, viz. (*a*) skid stack feeding; (*b*) connecting conveyor; (*c*) coil feeding.

(*a*) An investigation of the skid method disclosed this apparatus contained a very desirable feeding mobility, such as (1) reloading may be extended to approximately one-hour intervals; (2) reloading does not interfere with the line production; (3) the sheets can be separated before being transported to the receiver; (4) double-sheet feeding detecting devices are employed; (5) sheet feeding is controlled by the receiving unit; and, most important, (6) stock changing may be rapid and within the time limit of other line units. The photograph, Fig. 1, is a close-up view of an auxiliary feeding apparatus clearly showing the vacuum cup lifts, sheet-separator magnets and auxiliary stock rack. This type of feeder is also employed at the lithograph, blanking, lap and other processing units. Operating features are simplicity and versatility of operation. The limitations are the necessity for sharply defined sheet edges and reasonable care in bulk stacking.

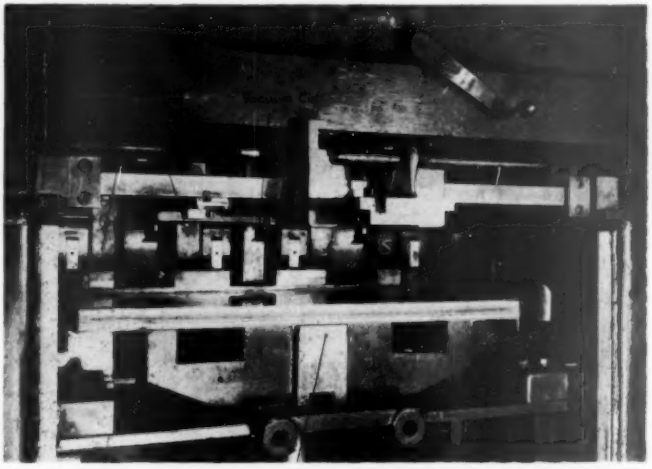

Figure 1

(*b*) The conveyor feed, Fig. 2, receives and transports the processed sheet directly to, and correctly times it for, the receiving unit. In case of an interrupted work flow, the sheets are automatically diverted until the preceding machine is stopped. This apparatus may also contain the lap-cleaning device. The conveyor is usually arranged parallel to the weld unit but working clearance is provided for adjustments. A right-angle connecting conveyor contains the controls for the machines and work stoppage. Operating features are the absence of the necessity of sheet separation and other inherent stacking difficulties. The limitations are of minor importance.

(*c*) When coil feeding the stock, it is sized to girth dimensions at the mill and later sheared to height in an arrangement shown diagrammatically in Fig. 3. The mill imposes an extra material cost differential for sizing, but this is considered good economy by some pail producers, especially when the mill tolerance is within safe diameter limits. In Fig. 3 the stock is threaded through the straightener rolls and continues through the lap-cleaning apparatus and shear until the leading edge contacts the gage. The pinch rollers function as an auxiliary feed for the stock. The coiler is adjusted to intermittently feed the required sheet height to the gage; the shear then operates on the dwell action of the coiler. Detecting devices eject short or long sheets.

The gage, *P*, is arranged to by-pass the sheets in case of machine stoppage or adjustments. After shearing, the sheet is transferred, at right angles, to the receiving unit where it is synchronized with the feeding apparatus of the receiver. Operating features are:

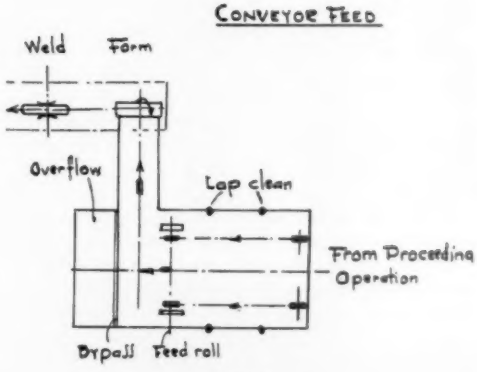

Figure 2

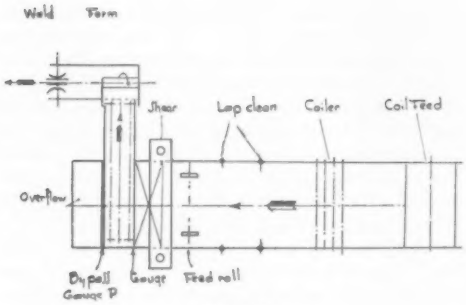

Figure 3

stock feeding difficulties are omitted, the coil grain pattern is consistent and scrap disposal is reduced to a minimum. There are a few limitations. It is important to note that lithographed stock cannot be accommodated in this arrangement. Moreover, frequent diameter changing implies a comparatively large coil stock inventory. Partly used coils and end cutoffs should also be considered when planning this arrangement.

SHEET FORMING

There are several sheet-forming methods for both manual and automatic work. Since the actual operation is similar, the important difference being in the automatic sheet control and transporting factors, these only will be described. It is important in automatic sheet forming that the leading girth edge is square with the height edges and, moreover, that the height-edge gage is set normal to the roll axis. An error at these points results in misaligned body-height edges or a tapered body. Although these may be corrected at the lap gage and the transporting device, more uniform performance at subsequent operations can be expected if the four sheet edges are maintained square before forming.

Figure 4, (*a*), (*b*) and (*c*), diagrammatically shows the generally accepted forming-roll arrangements. We have made a comprehensive investigation of these and other forming schemes and believe, for the task described in this paper, Fig. 4 (*a*) contains greater assurance of consistent performance. In operation, the sheet passes through the pinch rolls, the leading edge of the work contacts the deflector roll and is deflected downward in the direction of the arrow, until it comes to rest at the gage; the stripper positions the trailing edge on the opposite side of the gage. The pusher then advances to square the trailing height edges and transports the assembly to and through the welding station.

In lap gaging, Fig. 4, (*a*), (*b*) and (*c*), shows various relations of the gage to the forming rolls. At (*a*) the gage is positioned directly beneath, and slightly off the center of, the lower pinch roll. At (*b*) the gage has a similar relation to the upper pinch roll, but the body is pitched upward. In this arrangement a view of the vital functioning of the machine is obstructed by the body. At Fig. 4 (*c*) the gage is part of the feed line and is positioned as close as practical to the roll. A forming steel is positioned on the opposite side of the lower roll but set slightly lower than the feed line. The sheet passes through the rolls and is deflected downward as at (*a*). In this case, however, the trailing and leading edges do not overlap until stripped from the former rolls. This scheme is adapted for lighter gage stock and smaller diameters to avoid stressing the metal beyond safe limits. In all three cases, the gages are usually continuous from forming to welding.

Again referring to the (*a*) arrangement, we have conducted several experiments to ascertain if this gage continuity is important. We believe the principal function of the gage, at forming, is to correctly assemble the edges and space them to approximately, but not necessarily, the final lap width. It is, therefore, not essential to these functions that the gage continue the entire transporting distance. Moreover, by using a minimum gage length, edge abrasion is reduced and may be entirely omitted, which should result in a more efficient transporting operation. Based upon this reasoning, we positioned gaged segments at the leading and trailing edges of the work. These were made to retract at the instant the transporting apparatus gripped the work edges. This is described in greater

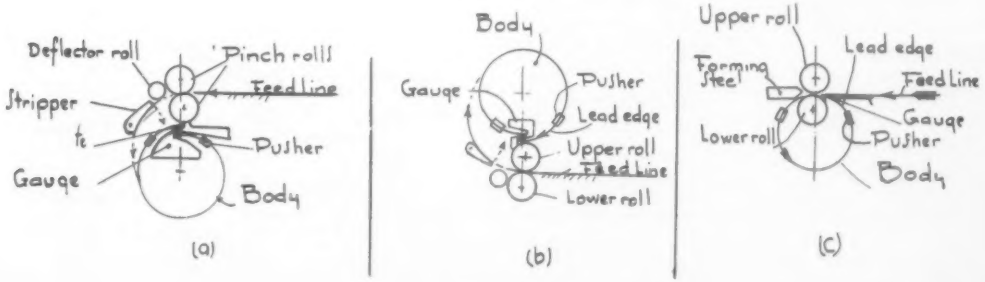

Figure 4

detail under transporting apparatus. This modification of conventional gage design provides a much greater flexibility of work movement for automatic equipment.

In body transporting from former to welder, the important function is not only to move the work from station to station, but also to retain the lap setting when the work leaves the gage and further to hold the height edges in line. In our preliminary analysis of these requirements, we believed that adequate performance could be expected if we deposited the formed body in a receptacle of identical dimensions which also contained the gage. The entire assembly was then to be moved to welding, and dwell there until the body was stripped from the carrier by the welding wheels. Although the apparatus performed as planned, we overlooked an important item, viz., the stripping time lag. This reduced the duty cycle too low for automatic operation. An attempt to overcome this objection with continuous chain feeding attachments was abandoned because of the difficulty in maintaining height edge and lap alignment, also attachment spacing uniformity

LAP THERMAL EXPANSION

Figure 5 (*b*) is a section along the line of seam; it shows the relation of the nuggets at 9 in. per second welding speed and the preheated increment, *i*.

Figure 5 shows a partial transverse center-line section at the welding wheels. The seam lap, *l*, is passing through the wheels in the direction of the arrow. The full pressure and weld current is concentrated on the mass $l \cdot t_1 \cdot x = v$, or the vitazone. When *v* is raised to fusing heat, the increment *i* (which extends from the nugget to a distance slightly beyond) has also been preheated to at least a forging temperature.

Referring to Fig. 5, (*a*), (*b*), the area $l \cdot i \cdot$ represents the preheated zone. This is surrounded by the cooler base metal *c* Fig. 5 (*a*). The behavior of the heat in this area is very similar to the moment curve of a cantilever beam of length *l* with the support at line *o;* this is also the lap width. Each impulse of heat and the forging action of the welding wheels expand the lap edge of the seam in the direction of *f*. The magnitude of this value, although not great, under certain conditions may cause the complete loss of lap before the weld is finished. As an example, at a weld speed of 9 in. per second 60 cycle current $^3/_{16}$ in. lap, 14 in. seam length and a $^1/_{1000}$-in. expansion per $^1/_2$ cycle, and expressing the welding time in current frequency, 60/9 = 6.66 cycles per inch of weld. We weld on half cycles or 2 × 6.66 = 13.32 welding impulse per inch. The spread = 13 x $^1/_{1000}$ x 14 in. = 0.152 in. in 14 in.; our lap width is 0.187 in. Moreover, a pressure reading adjacent to the weld station with the above operating condition indicated a tangential force of approximately 20 lb.

LAP CONTROL

From what we learned in Fig. 5 regarding lap spread during the weld and from other experiments, not noted, we conducted a rather extensive investigation to determine other causes for this expansion. Our data indicated thermal lap expansion to be the principal cause of this difficulty and, moreover, if we expect consistently satisfactory seams at automatic operating conditions, some means must be devised to control it. This is generally recognized, but there appears to be a lack of agreement with reference to the most efficient means of achieving it. This is clearly demonstrated in the following diagrammatic sketches of the apparatus most commonly used.

SPOT TACK

This method as shown in Fig. 6 employs two machines and operators. The first machine receives the formed sheet at the gage. The lap is assembled and the leading edge of the work pressed to alignment at stop. The electrodes tack the lap in position. This is repeated at approximately 4-in. spacings. The shell is then placed between the seam-welding wheels, Fig. 7, the current initiated and the shell, at the start, guided

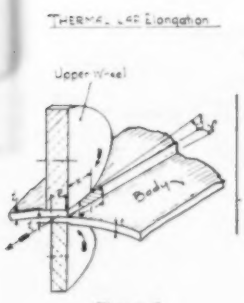

Figure 5

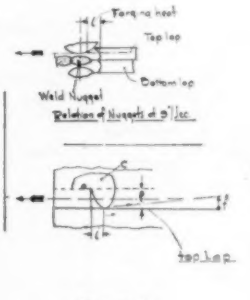

Figure 5a

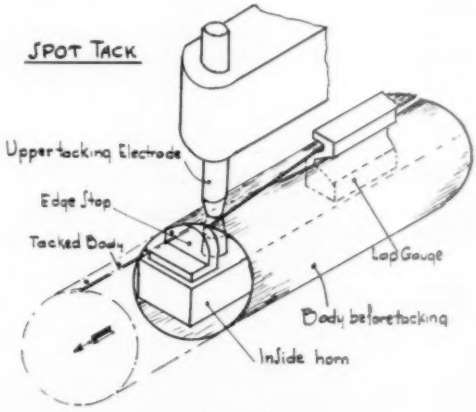

Figure 6

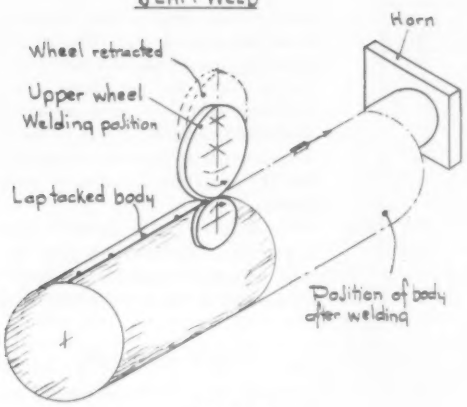

Figure 7

manually. When the seam is welded, the wheels are retracted and the shell withdrawn. In this arrangement, the lateral lap expansion explained in Fig. 5 is constrained between these spots and the internal stresses set up in the surrounding metal, while welding the seam, are also somewhat dampened by these spot anchors. Although this arrangement assures a uniform lap, it contains several unfavorable economic factors, the most important of which are high labor costs and low output. Where these units were combined in one operating machine the maintenance, operating and adjustments were too complicated for general acceptance.

SKEW WELDING WHEELS

This method of lap control sets both weld wheels at angles ϕ, α with reference to the axis alignment. This is expected to draw the laps inward as the seam passes through the wheels. Our experiments with this scheme were not very encouraging. With the aid of the formed rollers, however, the lap may be controlled successfully. That part of these rolls *a*, *b*, Fig. 8(A), tangent to the weld line provides the most effective lap

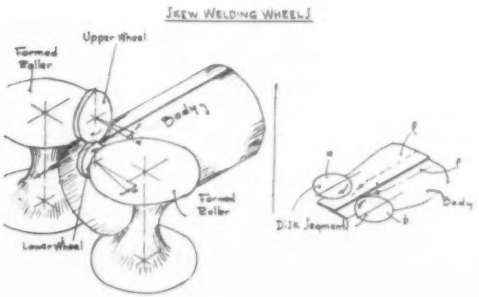

Figure 8

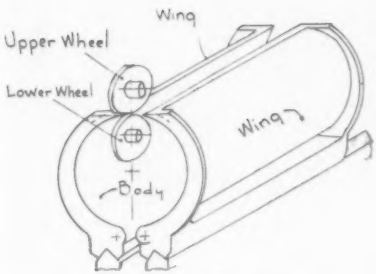

Figure 9

control. The radial movement of these disk segments forces the lap edges l, l_1 inward, as shown at arrows. These rolls are rather generally employed for this task. The abrasive action of these formed rollers on lithographed stock, however, frequently produces objectionable scratching. For this reason, they require special mounting and material treatment.

WING-TYPE WORK-HOLDING FIXTURE

In this arrangement, Fig. 9, the formed shell is deposited in a transporting fixture; the wings envelop the work and advance toward the welding wheels. The work is then stripped from the fixture by the power-driven welding wheels. The fixture then retracts to repeat the operation. An early development of this method employed traveling welding wheels and a stationary fixture. In both arrangements, there are several mechanical disadvantages; the most important objection is the comparatively low-duty cycle because of high inertial values. Although each of these arrangements contains some desirable characteristics, we believe the optimum equipment for this task should embody the best of these and also include consideration of the data contained in the table in Fig. 10, a description of which follows.

The data of Fig. 10 was obtained from welding experiments and shows the influence the factors listed have upon the lap width. These tests were conducted with No. 26 gage, 1010 SAE cold-rolled steel stock, $^{3}/_{16}$-in. lap by $13^{3}/_{4}$ in. long, except where otherwise noted. The work was held in position with formed rollers at the welding station. As an aid to a better understanding of the table, in the column titled "Lap Width," read downward in the $^{5}/_{16}$-in. column to the ordinate intersection of the diagonal. Following this to the left shows 0.031 in. loss per inch of length. The same diagonal followed to the right indicates this to be a 10% loss per unit length. Similarly, the results of the diagrammatic wheel-setting sketches at the bottom of the table may be ascertained. These results multiplied by the length of the seam equals the total lap

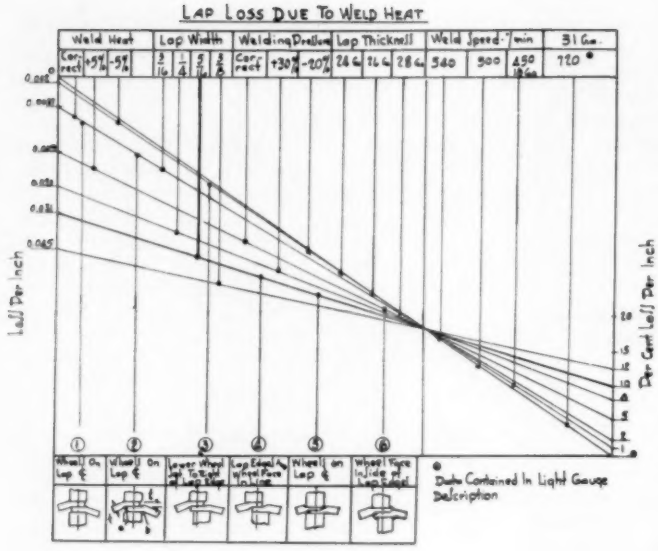

Figure 10

spread with the known conditions. In some cases, the lap was entirely lost before the end of the seam was reached; in other cases, a slight loss was noted.

The wheel and lap relations shown in Fig. 10 produced some interesting data as shown in the following:

(*1*) shows a normal wheel alignment. This is the relation used to obtain the data in the tabulation. The center line of the wheel and lap coincide.

(*2*) shows the lap thickness t_1 following the current initiation. Points *a*, *b* were believed to have a lap-spreading effect. This was not excessive as compared to the analysis of Fig. 5.

(*3*) The lower wheel is set to the right of the inner lap edge, the intention being to concentrate the welding pressure upon the outerlap edge. This was expected to produce a track and thus prevent splatter. The results were satisfactory, but cannot be maintained.

(*4*) The lap edge and the faces of the wheels are in line. In this case, the lap was entirely lost at 12 in. from the start.

(*5*) is a typical mash weld resulting from excessive pressure. This prevents splatter but accelerates the lap spread. This is a manufacturing requirement with some pail makers but requires more frequent wheel attention.

(*6*) In this scheme, the wheels were set to produce a slight track on both lap edges. It was expected to reduce the lap spread. Although there was a slight improvement in lap alignment, mechanical difficulties discouraged its use for production work.

Therefore, from this data, we should expect lap-seam control apparatus to include (*a*) correct welding current for the task, (*b*) correct welding pressure, (*c*) minimum lap width, (*d*) welding wheels lap centered, (*e*) lap to be preset before welding, (*f*) a mechanical means adjacent to the wheels to resist lap spread, (*g*) the wheels mounted on a normal axis with reference to the work, (*h*) wheels wider than lap width, (*i*) wheel perimeter cleared of foreign inclusions, (*j*) minimum friction at wheel-shaft journals and (*k*) minimum abrasion on work surface.

We believe Figs. 11 and 11 (*a*) embody these features and offer the most dependable assurance of obtaining continued high-speed production.

LAP GRIPPER

Figure 11 is a diagrammatic arrangement of the trailing portions of the work with reference to the fixed gage. The pusher contains the edge grippers *a*, *b*. As the pusher advances in the direction of the arrow, the grippers align the trailing edges of the work at *c* and *d*. The lap as set by gage is also retained at these points. The leading girth edges *e* and *f* pass through the front gage. The work advances until these edges reach the compression rolls *g*, Fig. 11 (*a*). These maintain the lap setting while welding and function somewhat similarly to the spot tacking previously described. Moreover, they also permit the pusher to be retracted and repeat the feeding cycle before the weld is completed. The duty cycle of the machine is thereby greatly improved.

Gripping the lap edges as described permits presetting the lap to compensate for thermal expansion and also virtually eliminates the necessity for excessive

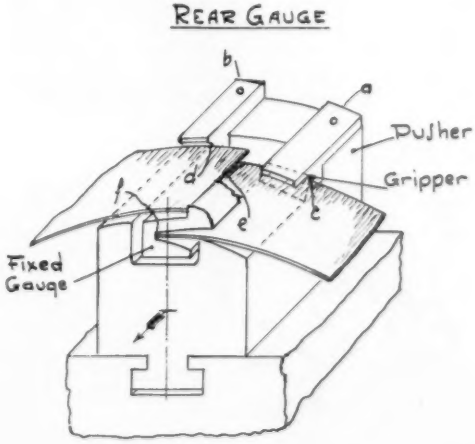

Figure 11

body supporting pressures and consequently lithograph mutilation. This apparatus, with slight modifications, may also be employed for producing constant diameters rather than constant lap sizing.

WORK EJECTOR

It may be of interest to add a note of caution with reference to the work removal following the weld. It is very important to continue the work control beyond the completed weld cycle to avoid any possibility of loss of lap relation to the wheel. Figure 12 shows two follower chains which were successfully employed for this purpose. It also provided a means to continue the welded body while still hot, through a seam-lacquering device. In this case, it may be noted that these chains contain pads. These approximate the body contour and also function similarly to the top segments of the hour glass or formed rollers usually employed and described in other operations. In any case, some means should be adopted to avoid the above difficulty.

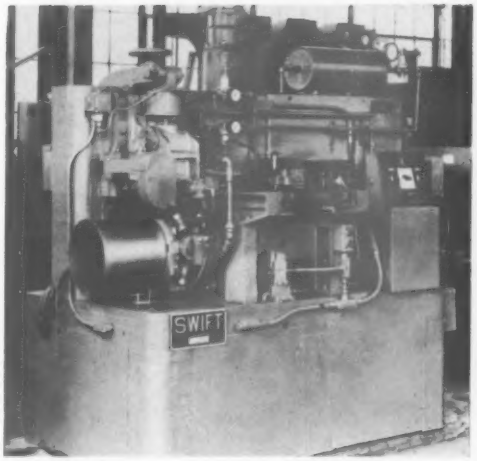

Figure 13

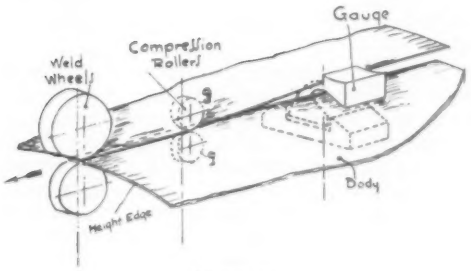

Figure 11a

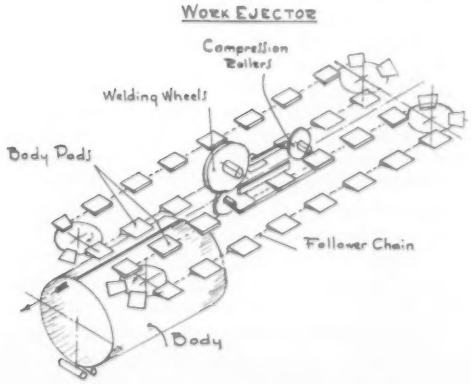

Figure 12

CO-ORDINATION OF FUNCTIONS

In co-ordinating the functional kinematics of the machine shown in the photograph, Fig. 13, we adapted the diagram shown in Fig. 14. This provided a reasonably simple method of proportioning the task time with reference to the respective inertia forces. In this case, our task is to roll-form a flat sheet 35 in. long, transport the formed shell 9 in. to contact the welding wheel, lap weld 14 in. of No. 24 gage mild steel, and eject the completed body in the processing time of 2.3 sec. per unit. Our maximum weld speed is 9 in. per sec. or 1.5 sec. for 14 in. From experiments, we decided on 0.5 sec. for forming; the inertia force of the work-transporting apparatus was found to be safe at $9^3/_4$ in. in 0.42 sec. This and other functions shown in the figure were arranged to produce a maximum duty cycle and, moreover, balance the metal passing through the inductive loop. This can be seen in the figure. At the weld curve, the welding wheels are at zero. As the work passes to the left of this point the metal in the loop decreases until the point *a* is reached; this is 5 in. from the start. The former curve then delivers the second sheet into the loop, thus producing a comparatively uniform inductive reactance. The roll strippers and the transporting apparatus follow the forming in sequence. The schematic diagrams at the margin show the relation of the work to the weld and forming stations, at the critical points. It may be noted that as a result of this timing analysis, the welding time was finally modified from 1.5 to 1.87 sec.

The duty cycle of this arrangement may be appreciated by comparing the trailing end of curve *1* at *b* to the complete advance of the second sheet at *c*. The time value of this 2-in. space is 0.087 sec. as compared to the total task time of 2.3 sec. or $2.22/2.30 \times 100 = 96\%$.

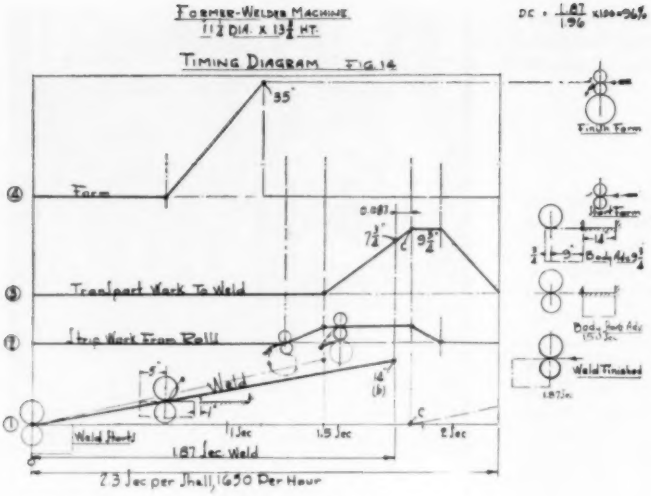

Figure 14

CONTROL OF FUNCTIONS

Each operation shown on the diagram is interlocked with the one preceding. The initiation of the work cycle occurs with the entrance of the sheet into the forming station. The functional control is, therefore, somewhat of a departure from the conventional cam shaft cycling common in all automatic production equipment.

In this survey, we have investigated the flat-sheet preparation for sizing and welding a pail body. We have also explored the processing requirements for a high-production quality package. With this data as a basis, we have developed an automatic unit which we believe is the optimum for container body-making of the character described.

Electrically, the current employed was single-phase, 60-cycle, tapped transformer and heat control, continuous current with electronic switching. Although no special effort was made to control the firing time in relation to the voltage or current wave, our results were commercially acceptable.

LIGHT GAGE STOCK

As previously stated, the stock of this investigation is up to No. 28 gage and 540 in. per minute weld speed. Lighter gages may be processed with reference to the mechanical co-ordination of the unit described, but electrically certain modifications are required. As an example, to seam weld containers of 100-lb. (0.011) stock at a welding speed in excess of 1000 in. per minute, 60-cycle, single-phase current, firing on full wave, welding this stock at the weld speed required would space the weld nuggets well beyond safe limits for a gastight seam. Moreover, the weld resistance as compared to the loop or throat impedance introduces other complications. We thereupon made an investigation of all known means of producing a welded seam to meet the requirements noted and one which would compare favorably with the lock and lap soldered side seam of the tin container.

a High-Frequency Current. We have found in this, although our nugget spacing difficulty would improve, certain undesirable characteristics of the alternating circuit increased with the frequency. Inductance remains essentially constant; therefore, the inductive reactance varies directly as the frequency. The resistance in the copper in the throat of the machine also increases with the frequency, the results of which are high power line demand and higher inductive heating of the steel in the loop.

b Saturable Reactor. The data on this apparatus disclosed numerous economic and maintenance problems and was, therefore, abandoned.

c Direct Current. This indicated encouraging possibilities; viz., the throat inductance does not affect the current.

Resistance voltage drops are improved because of better distribution through the copper. There are, however, objections to both mercury tubes and motor-generator conversions; viz., the mercury-tube voltage drop is constant at approximately 15 v. Our voltage requirement is in the order of 1.5 to 3 v. at the weld. Rotating generators are comparatively costly and they also involve commutating and armature difficulties. Storage batteries would cause switching problems and they also require frequent servicing and recharging. Direct or rectified current as produced with dry-disk rectifiers, however, satisfied our conditions. Our investigation disclosed two commercially available units for the task, copper oxide and copper sulphide magnesium. We adopted the copper oxide apparatus. This was connected to form a three-phase, full-wave rectifier. The inherent low-voltage and high-current characteristics produced entirely satisfactory results. Rectifier junctions may be paralleled to make a compact and reliable apparatus.

Figure 15 (*a*) shows a schematic diagram of the power circuit with tapped transformer on the primary side for heat control and switching apparatus. The "Y" and "Delta" windings show each bank out of phase with the other merely for sketching convenience.

On single-phase a.-c. welding, the transients produced by initiating the current on different parts of the voltage wave vary and, unless synchronous timing apparatus is employed, result in nonuniform welds at the

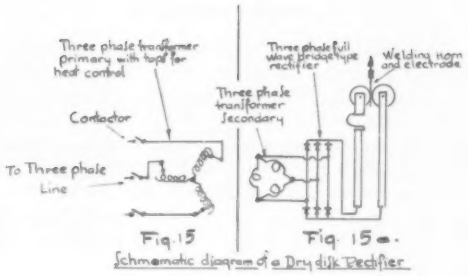
Three phase transformer primary with taps for heat control
Three phase full wave bridge type rectifier
Welding horn and electrode
Three phase transformer secondary
Contactor
To Three phase Line
Fig. 15
Fig. 15a.
Schmematic diagram of a Dry disk Rectifier

Figure 15

work. At these higher welding speeds, this item becomes very important.

With rectified current, and the three phases spaced 120 electrical degrees apart, the variable starting does not appear through the rectifier. The oscillogram, Fig. 16, shows this uniformity of direct current with reference to varied a.-c. starting. It is important, electrically, that this apparatus be located as close to the welding unit as possible; a safe distance would not be greater than 10 ft. 0 in. between the electrodes and the source of current. Our apparatus, with this spacing, produced very satisfactory welds at 1.5 v. d.-c. at the rectifier terminals. In a comparison of line demands between 60-cycle, a.-c., single-phase apparatus and this rectifier with identical specimens, with 15,000 secondary amperes, the a.-c. arrangement drew 111 kva. and the rectifier 55 kva. Moreover, the three-phase input causes less line disturbance than single-phase input.

The photo, Fig. 17, shows the welding station of the machine from which we obtained our light-gage metal data. We found that the conventional inner and outer weld wheel assembly would not meet the production requirements of the light-metal-container industry. The photograph is our solution of this problem. The main rotor revolves continuously. This comprises five complete weld wheel assemblies. Each of these contacts the body at the splined horn shown with restraining wings enveloping the work and horn. As the wheel contacts the work, the current is initiated and

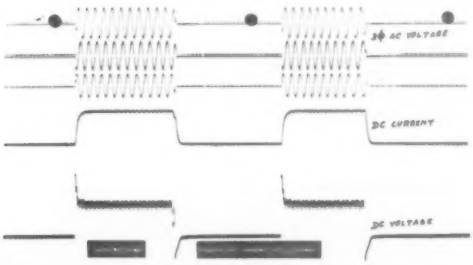

Figure 16

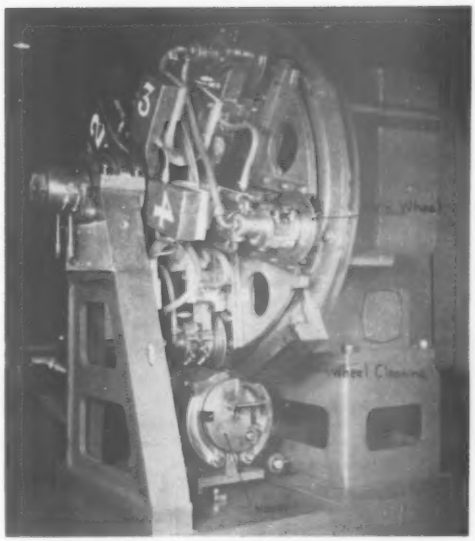

Figure 17

continues to flow until the wheel passes over the full seam length. The welding pressure is applied, by air, through the core of the rotor spindle. At the completion of the weld, the restraining wings are opened, the welded body ejected, the second body positioned on the horn, expanded to diameter, and the wings closed. These operations occur while the wheels move through the "dwell" arc of the rotor. The current is conducted to the rotor through specially designed brush contacts and controlled as previously described. With rectified current and this welding apparatus, our welding speed was limited only by a condition arising because of the greatly increased mechanical movement of the wheels. As the lap in contact with the wheel reached the plastic range of temperature, the upset pressure appeared to lag the current and perform its function of forging beyond our vitazone region.

In conclusion, we have presented the critical conditions inherent in this type of automatic equipment and offered well-proved methods of controlling the important functions. It has been shown, in the heavier-gage equipment, that the actual weld is not the most difficult problem of this fabrication. It has also been emphasized that to expect continuous automatic production, of a commercial quality, it is very essential to provide the machines with accurately prepared stock. We have presented a machine embodying noteworthy operating characteristics and a wide size range for the task of this investigation. Moreover, a practical method is shown for proportioning the task time of this and other automatic equipment.

For lighter-gage-container work, we have explored various means of improving the welding speed in the

interest of increased production. We have presented rectified current as the optimum for this task. We have also indicated the limiting factors of high-speed seam welding on lighter-gage steel and have suggested an objective for future investigation. There are specimens available for inspection of each of these containers. Arrangements may (upon request) be made to inspect the machines in actual operation.

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Trends in Electronic Nonsynchronous Resistance Welding Controls

by Stuart Rockafellow

ALTHOUGH the general use of resistance welding has increased in the past few years, by far the greatest concentrated increase has been in the automotive, refrigeration and associated fields. This increased use has been motivated by proof of increased production. As this use increases, the welding engineers are demanding more consistent results from the welding machines. Some of these demands involve the welding machine manufacturers and some go directly to the control manufacturers. The demands to the control manufacturer include more precise timing, faster over-all operation for gun welders, quick interchangeability of all panels, the addition of auxiliary units to standard panels to accomplish additional operations and means for giving his maintenance men better understanding of electronic circuits.

PRECISION TIMING

The general trend is toward the complete use of identical tubes in the timing unit. These are usually of the smaller type thyratrons such as the 502A, 2050 or the 2D21. By the use of identical tubes, the maintenance man has to carry only one type of tube in stock to service his control panels.

The characteristics of the smaller type thyratron tubes lend themselves well to welding control use. Many control manufacturers are using straight alternating current in the sequence panel and are using thyratrons as grid controlled rectifiers which will operate only on the positive half cycle. The timing is then always started on the same half cycle and, with recently improved relays to operate both the solenoid and weld control, the point at which the relay closes can be very closely controlled. The opening of these relays is automatically controlled by the last impulse of the positive half cycle giving accurate control for both pull-in and fall-out of the relays.

An example of the accuracy obtained by the use of relays to control the weld timing may be had by the specifications proposed by one large auto manufacturer. These specifications stated that the control should be able to start the weld current flowing at the peak of the voltage wave in order to prevent large line transients. Five different control manufacturers submitted panels, two with tube firing and three with relay firing, and all conformed to the specification. This same auto manufacturer requested that all panels should have a minimum sequence time of Squeeze, Weld, Hold and Off times and should be no more than 7 cycles. All controls submitted complied with this particular specification and some bettered these times.

These fast times with a minimum bottom of 2 cycles Squeeze, 2 cycles Weld, 1 cycle Hold and 2 cycles Off time would probably never be used in conjunction with each other due to the limiting speed of the welder gun operation. However, in many cases, one particular time such as Hold time may be desired as low as 1 cycle to effect quick gun release. By the design of these low minimum times, the minimum of any one may be used.

It is generally recognized that to accurately control the current flow through the welding transformer, the use of Ignitron tubes is necessary. For many years power-type magnetic contactors were used to control the weld current; however, frequent-maintenance and high-production down time have made the magnetic contactor obsolete for high-speed operation.

The Ignitron tube is a switch with no mechanical parts and with correct cooling and load will function for many millions of trouble-free operations. Because of the characteristics of these mercury-filled tubes, the weld current always breaks at the zero point of the wave, preventing high line transients on the break.

The present trend is to build all timing components plus the ignitron contactor into one compact unit. The cabinet containing these units may also house disconnect switches, fuses or circuit breakers. JIC, dust- and oiltight cabinets are often used in locations where oil and dust present a problem.

QUICK INTERCHANGEABILITY

The foreman responsible for the output on his particular line insists that the maintenance man keep his equipment in condition so that down time is minimized.

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Scheduled for presentation at the Thirty-Second Annual Meeting, A.W.S., Detroit, Mich., week of Oct. 15, 1951.

Down time on high-production lines can be a large part of the production cost with man-hours, equipment time and overhead all running into the final cost of the product.

With the high cost of down time, the control user insists that all control panels be quickly interchangeable. This sets a trend toward plug-in units wherever possible. By the use of quarter-turn, quick-acting fasteners and husky interconnecting plugs, the various panels may be removed and replaced with tested panels in a matter of a few seconds. The maintenance man can then carry the defective panel to his workshop and repair it under better conditions than those encountered on a production line.

The permanent wiring to the terminal strip may also go to husky plug and socket assemblies so that when any component panel is changed it is not necessary to touch the connections to the pilot, solenoid valve, weld contactor, etc.

Relays handling the solenoid valve and the control to the ignitron contactor have limitations as mechanical operators. Many welding engineers have requested that these relays also be made plug-in. This has been made possible by the use of heavy 10-amp. Jones plugs with the relays mounted directly on the plug. Then when the operation of a relay is questionable, the maintenance man can quickly change the relay in a few seconds.

Rectifiers used in the firing circuits of the ignitron tubes occasionally give trouble. Because of this, one auto manufacturer has requested that these rectifier units be made plug-in. This is done by mounting the rectifier on a small plate with a plug and securing it to the panel with a quarter-turn, quick-acting fastener.

AUXILIARY UNITS

One of the many problems which confront the plant welding engineer is to provide control panels for many different types of resistance welding applications. Frequently, one particular job will require only one additional timing function but by adding that one function, a complete new control panel is necessary. The present trend is to provide outlets in the main control panel and use auxiliary plug-in units to provide several additional functions. For instance, in the welding of auto bodies, it is sometimes necessary to use a long initial squeeze time in order to have the guns close around a large obstruction. Obviously this long squeeze time for each succeeding operation would slow up the work. The use of an additional unit giving another squeeze time will then allow a long initial squeeze time and all subsequent squeeze times can be made as short as necessary to only have the guns to a new position.

In many cases with gun welding, various thicknesses of metal are welded in the same running sequence. With only one heat time, the thicker metals will not be welded as well as the thinner metals. To overcome this problem, an auxiliary unit may be used to give dual weld time selection. This unit allows the operator to select whichever weld time he desires by the use of two or more pilot buttons. As the sequence is running over light work he can select the shorter weld times; then as the gun moves to the heavier parts, he can push the pilot button which gives longer heat time. The trend is to make this type of unit to plug in to the regular 3B panel.

The welding engineer sometimes has both gun and press welders in the same shop. It is desirable to use the same type of control on both types of machines. However, the press welder may require a two-stage foot switch and in many cases will use a release or platen delay time. The release time may be necessary in order for the points to be removed before the main platen is moved for mechanical clearances. The trend is toward making both the two-stage pilot and the release time as auxiliary units which will plug into standard panels.

Future developments will probably have standard panels which will accommodate forge time, preheat time, postheat time and additional weld stages all available to be plugged in as auxiliary units.

BETTER UNDERSTANDING OF ELECTRONIC CIRCUITS

The average maintenance man is becoming more interested in electronics. As electronic timing becomes more generally used, he is interested in both the theory and the practical aspects of the controls under his jurisdiction. To meet this demand for more information and to counteract the complexity of the newer control units, the control manufacturers should prepare better instruction manuals and offer free schooling for the maintenance men. Many of the control manufacturers are offering these services to create better understanding between the manufacturer and the user.

The Application of Spot and Seam Welding to Design

New requirements in aircraft design require increased power and higher speeds to join heavier alloy gages

by S. P. Jenkins and T. E. Piper

INTRODUCTORY SUMMARY

CULMINATING twenty years of design and production experience in the application of spot- and seam-resistance welding to aircraft structural assemblies by Northrop Aircraft, Inc., the authors wish to illustrate and explain the versatility and high quality consistency of spot and seam welds obtained with present-day equipment and techniques as compared with the quality consistency obtained from homemade resistance welding equipment of twenty years back.

The authors explain the application of resistance welding to structural and nonstructural aircraft components, especially emphasizing the application of spot welding to aircraft twenty years ago as compared with today.

New requirements in aircraft design propagated through the use of increased power and higher speeds have resulted in thicker, heavier alloy gages to be joined, requiring larger capacity equipment than in the former types of aircraft.

The paper explains the urgent need to establish research and development programs to furnish engineering with suitable application design data for resistance welding, presented to the engineers in a manner that a designer can evaluate the use of resistance welding on different types of aircraft such as pursuits, bombers and missiles, and the specific structural application on each. The requirement to establish design allowables for resistance seam welding will be emphasized, as at the present time there are no design values for seam welding. With the advent of missiles seam welding should be used in aircraft more than ever before.

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Scheduled for presentation at the Thirty-Second Annual Meeting, A.W.S., Detroit, Mich., week of Oct. 15, 1951.

HISTORICAL

During the past twenty years in the aircraft industry spot- and seam-resistance welding has become one of the major methods of fastening aircraft. The persistent efforts of the AMERICAN WELDING SOCIETY are greatly responsible for the present application and advancement of this welding process within industry. At this time we in industry wish to thank you.

Spot and seam welding has proved satisfactory for primary structural applications. Until the start of World War II very little spot welding was done in air craft. A few far-sighted engineers and production-minded people saw the possibility of using spot welding for fastening certain secondary structures of an airframe. Spot welding is the fastest and most economical method of joining in aircraft production and requires less tooling. Structural assemblies are just as rigid and strong as riveted assemblies if properly designed for spot welding.

The one thing we wish to emphasize at this time is that spot and seam welding is no longer just a slipshod method of fabrication for an assembly that has very little load on it. Most aircraft companies have in the past few years ran many numbers of tests to disprove this belief and in most instances have succeeded. However, we still have engineers who say, "Prove to us that spot welding is as good as riveting, etc."

The Northrop Gamma (Fig. 1) was built, in 1933. It was the first over-weather flying laboratory ever built and was used to pioneer stratosphere flying.

The Gamma was used for many purposes; for instance, China bought them for attack bombers to be used against the Japanese. TWA used them for transports. It was used by Lincoln Ellsworth on his antarctic expedition. They were flown by the U. S. Army.

At the time this airplane was built a set of wings were spot welded for test purposes only. This to our knowledge is the first time spot welding was tried on a structural member of an aircraft. Of course, we

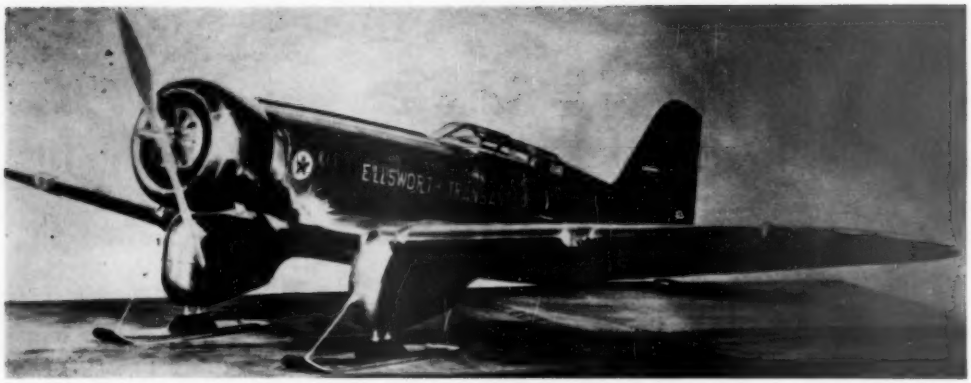


Fig. 1 Northrop Gamma Transport Airplane. First "Over-Weather Flying Laboratory" ever built and was used to pioneer stratosphere flying. Gammas sold to China were the first attack bombers used against the Japs

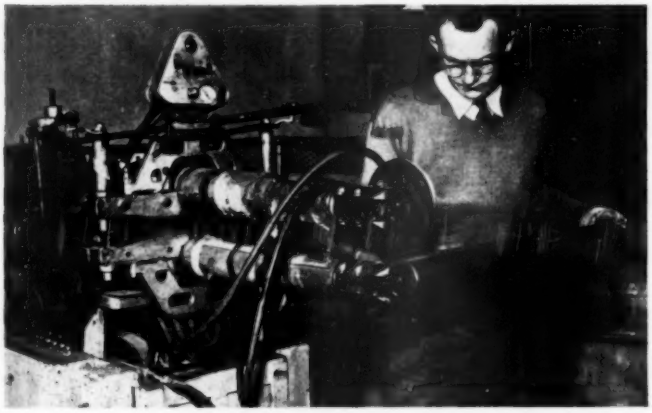


Fig. 2 Burns-Northrop Combination spot-weld and seam-weld machine designed and built in 1932 by Northrop Aircraft, Inc. Used for spot welding the wing assemblies of the Northrop Gamma Airplanes

needed equipment to do the spot welding; therefore, we had to build it.

The Burns-Northrop seam or spot welder (Fig. 2) was designed and built in 1932, to spot weld the stringers to skins on the set of wings that were used for test on the Northrop Gamma. This spot and seam welder was used on both Alclad and stainless steel and did a very excellent job, we are happy to say. The machine was used in production as recently as 1948. At that time new spot-welding equipment was installed. You will note in Fig. 2 the unique gear-driven seam weld wheels internally cooled by water. This we believe to be the first internally cooled seam weld wheels ever built. This spot welder was single-phase rocker-arm type, capable of spot welding 0.040–0.040 in. thickness of aluminum Alclad, having air pressure capacity of 800 lb. on the electrodes. The arms were automatically raised or lowered by an electric flip switch. The quality of spot welding produced by this machine was inferior to present-day specification requirements; however, the Northrop Gamma test wings that were spot welded on it proved to have high structural integrity even though many spots had evidence of cracked welds and poor penetration due to lack of quality consistency experience at that time. We wish to bring to your attention that today, twenty years later, C.A.A. will not permit the spot welding of stringers or hat sections to wing skins on commercial aircraft because of the nonexistence of substantiating test or service data. Please realize the seriousness of this lack of data on the eve of missile design and production.

The P-61 Black Widow (Fig. 3) was the first airplane ever built exclusively for night fighting and the first Air Force fighter designed and built subsequent to Pearl Harbor. Early in 1942 when we started production on the P-61 Black Widow airplane, a twin boom

Fig. 3 P-61 airplanes, night fighter built by Northrop Aircraft, Inc., 1942. The tail booms on this airplane are spot welded

Fig. 4 YC-125 rescue transport built by Northrop Aircraft, Inc., in 1948 with all spot-welded wings except for the assembly attach points

night fighter, we started evaluating the possibility of spot welding the two booms We were granted permission to try a set for test only. We proved that primary structures could be spot welded on an aircraft and be just as rigid as a riveted assembly. This spot welding of the P-61 booms reduced the cost from $1200 for a riveted assembly to $500 for a spot-welded assembly, a savings of 60%. A savings of many man-hours was appreciated, which was very vital in those trying days.

Northrop built close to 1000 of these airplanes which were in every theater of war. No record is available of our spot-welded booms failing under any condition that was encountered. Many of the P-61's and F-15's, which are photo reconnaissance planes similar to the P-61 in looks and having spot-welded booms, are still flying every day and are still structurally sound. In spot welding the tail boom of this interceptor airplane our resistance welding engineers set into progress a method similar to the quality control we now employ at Northrop (Figs. 11 and 12).

PRESENT APPLICATIONS

The Northrop C-125 Raider (Fig. 4) was first built in 1946. The Raider has ability to take off within 700 ft. carrying a useful load of more than five tons. The C-125 has spot welding on all the wings. Prior to spot welding the wings, spot weld tests were made on panels representing a section of a wing with stringer to skins, the skin range being from 0.016 to 0.016 in. 24STAL to 0.040 to 0.040 in. 24STAL. The stringers used were of a *Z* section and also hat sections of 24STAL were used. Upon completion of these tests we had proved we could spot weld thin gages of aluminum to heavy stringers and hat sections without warping, clanning or buckling. The elimination of warping, clanning and buckling when spot welding thin gages of aluminum skin to heavy stringers and hat sections of the wing was accomplished by using a reverse or concave contour to the outer surface. This introduces residual stresses into the assembly at time of flattening or bending to the convex position that neutralizes the spot weld shrinkage stresses that promote clanning under normal spot weld position.

The F-89 Scorpion (Fig. 5), an all-weather interceptor, is now being built by us for the Air Force. The F-89 has two Allison J-35 power plants that push it along over 600 mph. The F-89 is one of the most heavily armed modern planes in its class. We are proud to say that the Scorpion has a great many assemblies spot welded, one of these being the engine access doors shown in Figs. 6 and 7. These doors comprise part of the fuselage of the fighter. In being a part of the fuselage that houses the power packet, it should be noted that they have to be very rigid and possess a smooth airfoil surface in every respect since they are part of the structure. These doors were subjected to a number of tests before they were allowed to be used since they were to be spot welded and are classed as a primary structure. We would like to point out that the spot-welded doors met every requirement that they were subjected to in testing. The doors are quite large as you see in Fig. 6. The smallest is approximately 3 ft. long and approximately 6 ft. in width. The largest of these doors is approximately 7 ft. long and $4^1/_2$ ft. wide. The number of spot welds vary from the smallest door which has close to 1800 spot welds to the largest which has about 2760.

Other major structural parts are the keel or the backbone, part of the nose skins are spot welded, and the main landing gear door which is part of the wing structure when in flight. These are only a few examples; however, there are very few assemblies on the F-89

Fig. 5 Northrop F-89 Scorpion all-weather interceptor, carries six 20-mm. cannon making it one of the most heavily armed modern planes in its class. Two Allison J-35 power plants push it along over 600 mph.

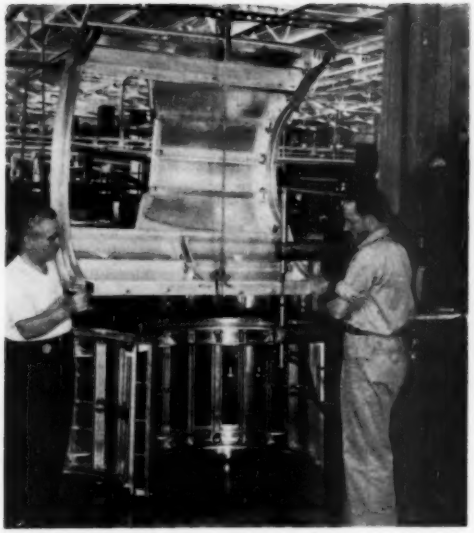


Fig. 6 F-89 engine across door being spot welded on a three-phase type spot-welding machine

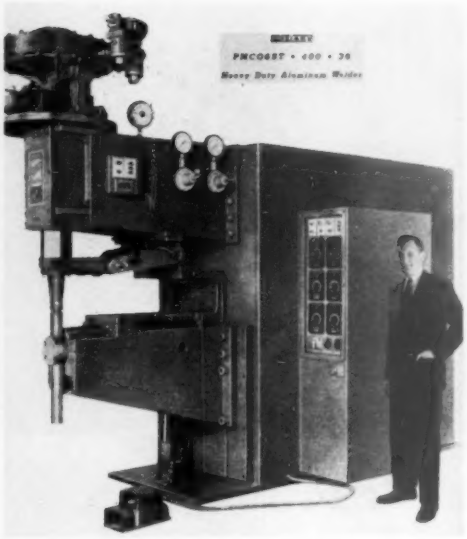


Fig. 8 Latest design of resistance spot-welding machine by welding manufacturer. Capacity of this machine is 0.186–0.186 24ST AL to MIL specifications

that do not have some spot welding on them. We believe the F-89 has more spot and seam welding on it than any other airplane in its class. We feel that had we been able to present spot weld structural application data to our design engineering considerably more spot welding would have been done on this twin jet all-weather fighter. However, the man-hours required to accomplish the great amount of testing which would be required to furnish the engineering data were not available on this contract. Once again we wish to emphasize the necessity for the AMERICAN WELDING SOCIETY'S support in securing this spot weld application data.

As mentioned above, aircraft design is changing rapidly, accelerated by the tremendous and rapid development in increased horsepower available. Let's look back to the twin-engine fighters of World War II that carried two 2000 hp. reciprocating engines having available 4000 hp. in all, as compared to the twin-engine jet fighters of today with their much more

Fig. 7 Side view of the F-89 showing engine access doors attached to the airplane. These spot-welded doors are approximately 6 ft. in length and 7 ft. in width. There are 8 doors to an airplane

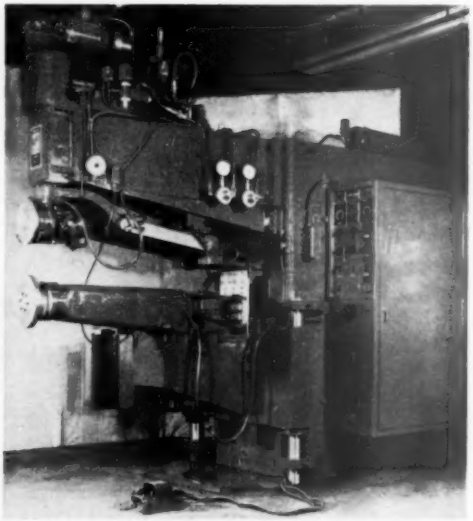


Fig. 9 One of the largest combination roll seam spot welders built to date for the aircraft industry by welding manufacturers to meet MIL specifications. Rated capacity of this machine is to weld two pieces of $^1/_8$ to $^1/_8$ in. 24ST AL and 75ST AL

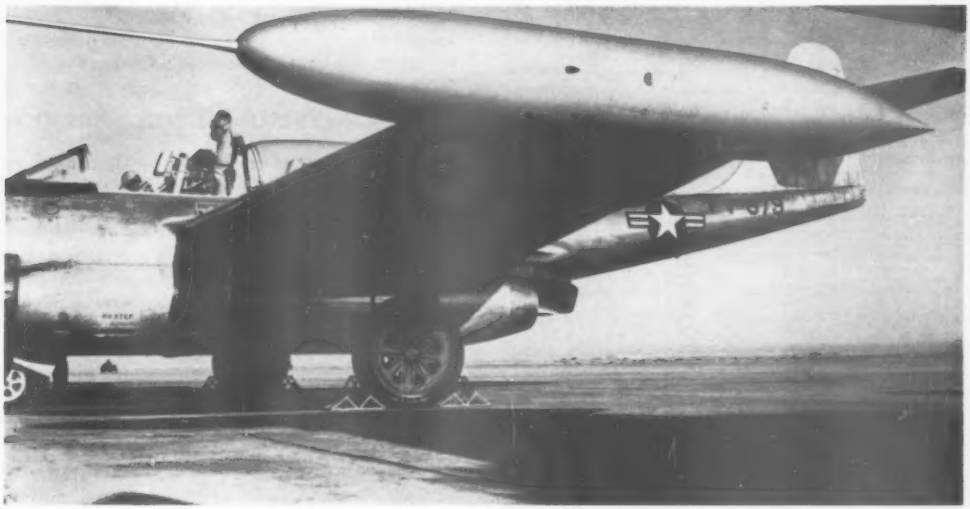


Fig. 10 Wing tip tanks on the Northrop F-89 all-weather fighter. These tanks are a permanent spot- and seam-welded structure. The tanks are approximately 21 ft. long and 37 in. in diameter and are tested under pressure of 101 psi., having the greatest welding thickness of 0.206 in.

powerful engines. The structure must be rigid to withstand the high speeds and therefore heavier or thicker gages of aluminum must be used. This necessitates the design and construction of resistance or spot and seam welding equipment beyond the capacity ever dreamed of in World War II. As for example, the tip pod tanks of the F-89 fighter (Fig. 5) have over-all thicknesses of 0.246 in. to be spot and seam welded. The latest spot-welding equipment of this type (Fig. 8) is a three-phase machine. Minimum and maximum gages spot welded on this equipment to MIL: specifications for aluminum alloys are 0.051–0.051 in. up to 0.186–0.186 in. of 24 or 75STAL. Also, the seam-weld equipment (Fig. 9) that has a maximum thickness capacity on 24 or 75STAL to MIL specification is 0.125–0.125 in. You may be interested to know the size of this seam-welding equipment. This machine is approximately 16 ft. in height and 18 ft. long, having a throat depth of 60 in. The resistance-welding manufacturers must at this time be given a vote of thanks for the interest and efforts they are exerting in fulfilling the requirements of a rapidly advancing industry.

The wing tip tanks on Northrop's F-89 all-weather fighter (Fig. 10) are partly responsible for its deadly power. These tanks increase the fighter's range, as each tank is attached permanently to the end of the wing. This tank, measuring about 20 ft. long and slightly more than 2 ft. in diameter, is constructed of 61ST material and is processed and welded to our standard resistance welding procedure. After completion of this tank it is tested for leakage under 10 psi. pressure and is not acceptable if any leaks are found. Several years ago it would have been impossible to fasten and seal this tank by resistance welding alone, but due to the new developments in equipment and practices we are able to accomplish it today.

As stated before, the tanks are completely welded by spot and seam welding. Since these tanks have to be airtight they are a perfect example of application that represents the importance of spot and seam welding as it is used today in primary structures design. The tanks far exceeded the normal expectations. Spot and seam welding proved to be the fastest and most practical way of manufacturing. At this time we wish to again emphasize the importance and necessity of securing design allowables for seam welding as no engineering data are available at this time.

Figures 11 and 12 show typical Quality Control Charts on a three-phase spot welder These quality control charts are on every machine for each thickness combination and alloy spot welded. The chart has an established machine setting which is made by Process Engineering. The setting is made from a 35-weld specimen sheet in succession. Five of these specimens are selected at random and micra sections are made of these. Before this quality control chart may be used the test values must meet MIL specifications and Northrop Process Engineering Spot Weld Bulletin SW-2C.

After the quality control chart has been approved by Process Engineering for a machine, company Inspection requests three specimens at the start of a production run and every 30 min. thereafter until the production run has been completed. If any one of the shear specimens being tested by Inspection does not meet the limits established for quality control, the spot-welding machine is immediately taken out of production until it has been corrected.

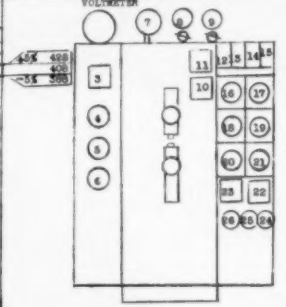
SPOTWELD SCHEDULE — FORM #2C

MACHINE TYPE: SCIAKY PMCR8T2 NO. 10 — CERT. DATE?

MATERIAL	75 S	75 S	75 S	
THICKNESS (IN.)	.032	.032	.032	
CONDITION	T	T	T	
AVERAGE (SHEAR STRENGTH)	UT. 605	RANGE	UL 141	
	X 550		R 56	
	LL 495	MIN. AVG. SHR. ACCEPTABLE	362	
ELECTRODE RADIUS	UPPER 4"	ELECTRODE	UPPER Wl. A	
	LOWER 10"	MATERIAL	LOWER Wl. A	

ADJ NO.	CONTROL	DIAL SET.
1	Arm Spacing	13½
2	Throat Depth	32
3	Weld or P.M.	Weld
4	Weld, Phase Shift	88
5	PM8 or Post Heat Phase Shift	16
6	Weld Ver. P.S.	90°
7	Contact Cage	33
8	Pr. Cage #1	36
9	Pr. Cage #2	16
10	Con. Cage Switch	On
11	Var. or Con. Pr.	Var.
12	Repeat or Non R.	Either
13	Ign. Tube #8	On
14	Ign. Tube #7	On
15	Pre-Heat Off-On	Off
16	Squeeze Time	30
17	Pre-Heat Time	0
18	Weld Interval	0
19	Weld Time	10
20	Off Time	30
21	Re-Comp. Time	30
22	Post-Heat, Off-On	On
23	Single or Mult. Impulse	Sing.
24	Post Heat Time	6
25	Cool Time	1
26	Heat Time	1

VOLTMETER



Note: 1. Observe machine manufacturers instructions stamped on plate on upper right hand corner of machine frame.
2. Use adjustment #4 to vary voltage. Keep voltage within plus or minus five percent limitations as shown.

Process Engineering.

Fig. 11 Process quality control spot weld schedule. The objective of this is to establish consistency of production spot weld quality

One of the major problems in aircraft spot welding is in development of good, reliable spot welders or operators. No matter how good and how accurate the equipment may be, a poor operator can make satisfactory production welding impossible. Upon the operator's shoulders rests the responsibility of properly setting the machine for the various combinations he has to weld. He is solely responsible for his or her work. It is up to the spot welder to see to it that the work that comes to him for spot welding is clean and properly fitted together. He should reject and refuse to weld any work brought to him that does not meet the spot welding specifications of his company or the MIL specifications.

The first step in training a new man to operate a spot-welding machine is to teach him the basic welding principles involved in spot welding. He should know a few simple facts about elementary electricity. It has been found when you acquaint the new personnel with these facts they seem to take more of an interest in their work from all standpoints. He should be given tests periodically by the company in order to keep abreast of the latest developments and requirements.

NEW ENGINEERING DESIGN REQUIREMENTS

Spot welding, when considered from the point of view of design, is merely one of the many structural fastening methods at the disposal of the designer, and which can be used by him in a great variety of ways depending both upon the purpose of structure and on his particular pattern of design stresses. It would obviously be presumptuous to attempt to dictate or even advocate here any one type or method of design as superior to others. There are, however, two aspects of spot welded design which should be presented to the structural engineer to assist him in a better understanding of the peculiarities of spot welding as a method of joining.

The first aspect is that of the physical properties of spot-welded joints. A structural designer has a right to know and can rightfully demand the unit strengths which can be expected from a joint made by any one process when subjected to various types of static and dynamic stress. In case of a method of joining such as spot welding, which is composed of a number of individual points of attachment, he has a further right to expect a knowledge of the best or the most efficient pattern or combination of individual points of the attachment for the desired strength of the joint. It is unfortunate, however, that this aspect of spot welding is still in the course of development and very little in the way of definitely established facts is now available. Accordingly, only the barest essentials of this subject can be presented at this time.

The second aspect of the spot welded design is based primarily on the fact that the chief advantage of spot

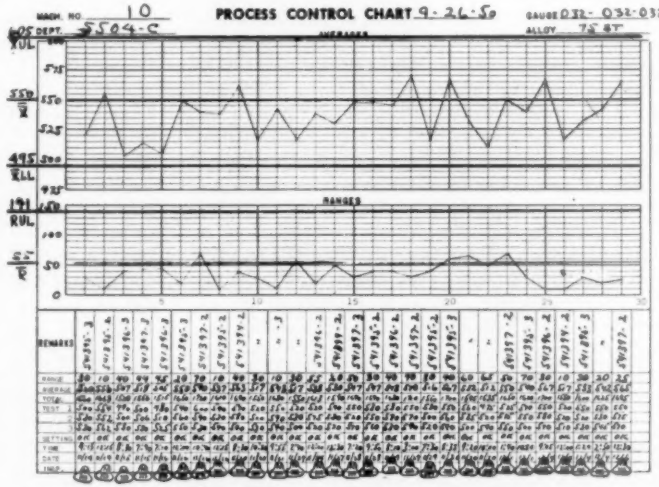


Fig. 12 Process control chart. It records the consistency of spot weld strengths produced by the equipment

welding as a structural fastening method is its comparative ease and rapidity of production. This characteristic, however, is contingent upon adherence to certain simple rules of design, the word design here applying primarily to the details of the joint used. Thus a comparatively minor difference in the detail of a joint may spell the difference between making the joint essentially easy and suitable for spot welding or extremely difficult and expensive. This second aspect of spot welded design deals then primarily with furnishing the structural designer with the facts regarding the scope and limitations of spot welding in so far as its ability to join rapidly and cheaply is concerned.

It will be helpful from the standpoint of design for economical production to review some of the comparatively recent advances that have broadened the application of resistance welding. The problem of power supply has been greatly simplified by the development of stored energy systems, off series and shunt capacitors, and the application of pulsation welding techniques. Electronic controls have provided a means of utilizing extremely short time intervals for welding, resulting in higher speeds and better weld quality. Postheat procedures and improved surface cleaning procedures have greatly widened the range of metals and alloys that can be resistance welded satisfactorily. X-ray testing, oscillograph controls, current regulators and other developments in instrumentation have made it possible to maintain a high degree of consistency in weld quality.

Once again we wish to encourage you, the AMERICAN WELDING SOCIETY, to continue with your tireless activity of advancing welding techniques and applications in production and encouraging the test work to secure engineering data necessary to further the application of resistance, spot and seam welding to airplane and particularly missile design.

Metallurgy for the Welding Student

Metallurgy is taught in technical high schools so that students may have a fundamental knowledge of metals and their use in industry

by J. D. Paterson

IN ANY large manufacturing area as Detroit, Mich., there is always a demand for some special training for men in industry. In this area there has been a demand for metallurgical training for men in many fields but welding is one of the foremost.

In studying the enrollment of the night-school classes in welding and metallurgy, a definite relation can be seen, that is, when the enrollment is high in welding the same is true in metallurgy.

Metallurgy is taught in technical high schools in order that the students may have a fundamental knowledge of the different irons, steels and alloys which are used in all industries with special emphasis upon the automotive, aircraft and tool industries.

In the fall of 1920 the first course in metallurgy was offered at Cass Technical High School. It was a lecture course but in succeeding years the laboratories were equipped.

In 1923 some metallographic equipment was purchased and later the heat-treating room and tensile-testing machine were added.

METHOD OF INSTRUCTION

Metallurgy requires classroom instruction, with movies, lantern slides, laboratory work and demonstration work performed in the laboratory.

The time devoted each week to classroom is three 45-minute periods and to the laboratory two 90-minute periods. In the night school the class meets two nights a week for $2^1/_2$ hr. and when they have completed 90 hr. of satisfactory work they are entitled to credit in the course.

At stated times each student is required to hand in a written lesson on the subject matter which has been discussed at previous meetings of the class.

The laboratory work is individual as each student is required to prepare his own specimens for microscopical examinations, make drawings of the same and make photographs from a special set of negatives which give him something different from the usual routine

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Fig. 1 Adjusting metalloscope for making a photomicrograph

laboratory work. I have met students years later who tell me they still have their photomicrographs. It is surprising the amount of time some students spend in preparing the cards on which they mount the prints.

LABORATORY EQUIPMENT

At Cass Technical High School we have well-equipped laboratories. The equipment consists of one large and one small photomicrographic cameras, six metallurgical and one binocular microscopes, five polishing machines and one grinder. In the metallographic laboratory and heat-treat room we have Rockwells, Brinells and a Scleroscope.

SUBJECT MATTER

The first part of the course is devoted to studying the manufacture and uses of irons and steels. In this work the student will become familiar with the advantages of some steel over others and the reason for the variations in price of the different materials.

Fig. 2 Making a tensile test on a steel specimen

In the laboratory the student is studying the structure of wrought iron, low- and high-carbon steel in the annealed state, gray cast iron and malleable iron. In the microscopical examination the student can gain information why these different ferrous alloys have varying physical and chemical properties.

The student is next given instruction in defects which may be found in ingots, castings and rolled and forged steel.

The heat treatment of steel is introduced with thorough instruction in the thermal transformations in pure iron and steels with special emphasis upon the structural and physical changes.

With this foundation annealing, normalizing, hardening and tempering are studied. The heat treatments given to some parts of an automobile or truck is thoroughly studied so the student will see the reason for the treatment.

In the laboratory the student is required to harden two pieces of A.I.S.I. C1040. One is drawn at 350° F. and the other is drawn at 1000° F. He then prepares them for microscopical examination.

After being checked by the instructor, the specimens are checked for hardness on the Rockwell and Brinell. A report is then made on a standard sheet.

With this knowledge the student is given a weld which may be an arc or gas weld. He always wants to examine both types of welds to make a comparison in the structure. Some students make welds at work, using various welding rods so they can make their own investigation.

Many members of the classes have some knowledge of case hardening. Some time is devoted to the different methods of producing a hard wearing surface and tough core. At this time the McQuaid-Ehn grain size determination is introduced. This test gives the reason for two steels with the same chemical analysis responding in different ways to heat treatment. The student also realizes the importance of rimmed, semikilled and killed steels.

In the laboratory he polishes a piece of carburized steel which is not heat treated. The first etching is in nital and after measuring the depth of case it is repolished and etched in sodium picrate.

A tensile test is made on a standard test bar and the student is required to plot the stress-strain graph from the extensometer readings and calculate the proportional limit, yield point, tensile strength, elongation and reduction of area from data obtained in making the test.

The alloying elements are studied thoroughly with stress on the effects of alloys upon the properties of the steel. The various alloy steels used in this community are discussed. Since World War II some steel fabricators have run into considerable trouble in performing certain operations because of the residual alloying elements present in the metal.

The commercial alloys of copper, aluminum and magnesium are studied. The use of these alloys in Detroit is most important because of the application of them in the construction of the automobile. Many of these

Fig. 3 Heat treating test bars

alloys are joined together by some welding or soldering process.

AUTOMOTIVE CURRICULUM

Some of the students in the Automotive Curriculum specialize in welding as they may take extra subjects if their scholarship is satisfactory.

Automotive Curriculum

10B	*Credit*	*10A*	*Credit*
English 3	5	English 4	5
Algebra 3	5	Geometry 1 (plane)	5
Machine Fitting	5	Engines & Chassis	5
Mechanical Drawing	2½	Chemistry 1	5
Health Education	2½	Mechanical Drawing 4	2½
		Health Education	2½
	20		25
11B	*Credit*	*11A*	*Credit*
English 5	5	English 6	5
Geometry 2 (plane)	5	Trigonometry	5
Automotive Electricity	5	American History	5
Chemistry 2	5	Welding	5
Mechanical Drawing	2½	Physics 1	5
	22½		25
12B	*Credit*	*12A*	*Credit*
Geometry 3 (solid)	5	English 8	5
Garage Practice	5	Engine Testing	5
American History 2	5	Civics	5
Physics 2	5	Metallurgy	5
Elective	5	Elective	5
	25		25

After he has completed the Automotive Curriculum he is eligible to enter universities or colleges of engineering. He is grounded in the fundameptals, principles of mechanics and science and a thorough knowledge of the different units which go to make up an automobile or truck.

The school also has an Aeronautical Curriculum in which the student may specialize in welding.

RESULTS

After many years of teaching I find this metallurgical training has been most profitable to many students as they have been able to advance to a higher position or go into business for themselves. Some of the night-school students have made a great sacrifice of time to take the metallurgy course, not only once but in a few years they may come back and enroll for a refresher course. Some students state they know that in working with metals, as welding, they can not do certain things but do not know the reason.

In the last few years there has been a drop in the number of students enrolling in the technical courses at Cass Technical High School and some have given the explanation that it is due to the security the worker has in his job because of his seniority.

There has been considerable opposition to metallurgy being taught in high schools by men within our school system and by college and university professors. Most people think of metallurgy dealing with extraction of a metal from an ore. This is true in some localities but in this area we are more concerned with heat treatment and mechanical working of metals.

Some of the large manufacturing concerns in this country have considered metallurgy so important that they installed their own courses so their help would have a better understanding of their daily work, working with, and heat treatment of, metals. The Ford Motor Co. offered metallurgy before any of the technical high schools of the country did. Every year more high schools are offering courses which are known as Metallurgy or some of its related subjects. This shows the ever-growing importance of the subject in this technological age.

High-Speed Consumable Electrode Machine Welding for Aircraft

Techniques developed for satisfactory machine welding using an inert gas-shielded consumable electrode with speeds many times faster in aircraft application than those previously used

by Bernard Gross and Robert A. Smith

WELDING may be regarded as a process which joins materials by fusion under a variety of conditions. A most significant condition is the speed of welding. It affects the economy of the process, the extent of distortion and the residual stresses, the mechanical properties and microstructure.

When manual arc welding replaced the slower gas torch, speed of welding was materially increased. Machine arc welding, using the tungsten electrode, in recent years has permitted an increase over manual arc welding speeds. A recent development in machine welding is the inert gas-shielded arc using a consumable electrode. Satisfactory machine welding, using the consumable electrode, can be accomplished at speeds many times faster than those obtained when using the tungsten electrode type machine welding.

The static mechanical properties obtained by the

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Table 1—Comparison of Speeds Using Various Welding Processes. Material 0.040, Stainless Steel; Type of Joints, Standing Edge, Butt and Lap

Type	*In. per minute*	*Speed comparison*
Gas torch	2	X
Arc manual, inert gas shielded	4	2X
Arc machine, tungsten electrode, inert gas shielded	15	7½X
Arc machine, consumable electrode, inert gas shielded	200	100X

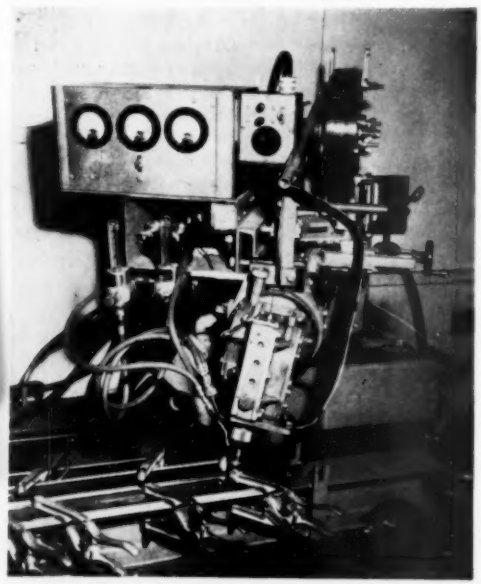

Fig. 1 Inert gas-shielded consumable electrode welding machine, capable of welding speeds up to 250 in. per minute

Fig. 2 Welding machine setup with instrumentation

consumable electrode type are comparable to the tungsten electrode welding while the micro-porosity is, at the higher speeds, somewhat greater. This process is applicable to aluminum and stainless alloys.

A high-speed automatic inert gas-shielded arc-welding machine was described in the April 1951 WELDING JOURNAL by G. L. Hoglund and is shown in Fig. 1. This machine is made for d.-c. operation, with a consumable electrode, of either aluminum or steel alloys and is capable of welding speeds up to 250 in. per minute. In the past year, it has found use in welding aluminum alloys having a thickness of 1 to 2 in. and stainless steel in the order of $^1/_4$ to $^5/_{16}$ in. thick.

In order to evaluate this method of welding when

Fig. 3 Type of specimen used in test—3 x 30 in. of gages from 0.025 to 0.125 in.

Fig. 4 Cross section of stainless steel type 302½ H alloy showing weld bead and adjacent parent metal area containing carbide precipitation

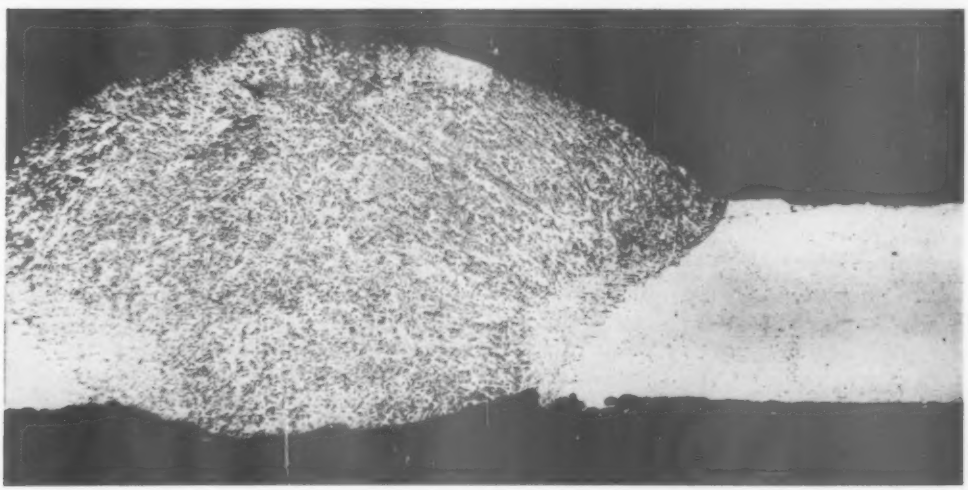

Fig. 4-A Cross section of stainless steel type 304 in. 0.025 gage showing weld bead and an adjacent parent metal without carbide precipitation

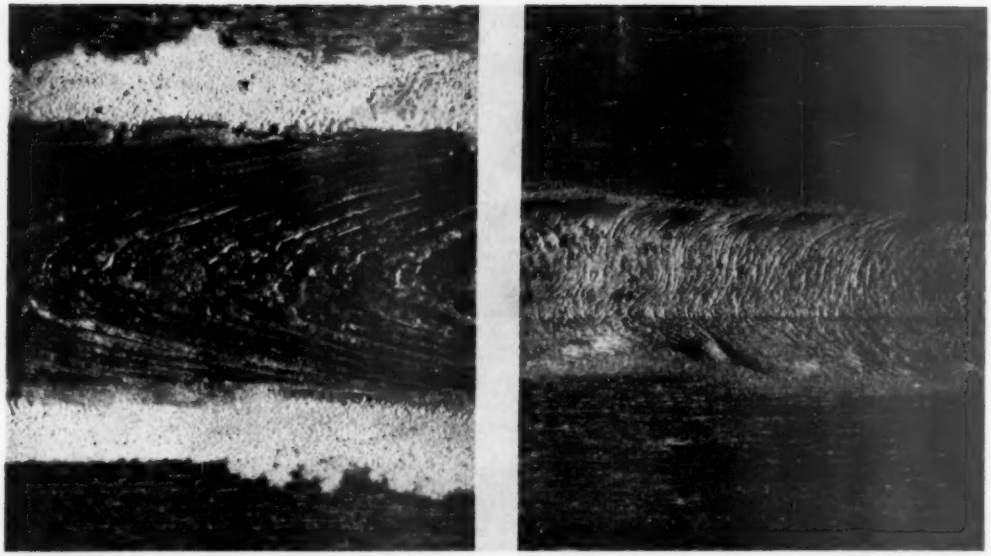

Fig. 5 External appearance of bead and underside of aluminum alloy 61ST-4 welded by the consumable electrode machine

using light gages, a test program was carried out at the Rohr Aircraft Corp. in Chula Vista in collaboration with The Linde Air Products Co.

The test setup with instrumentation for temperature and voltage measurements is shown in Fig. 2.

Materials welded were 3SO, 61ST-4, 61ST-6, 75ST-6 and 302 stainless $^1/_2$ hard. The aluminum alloys in 0.064 gage were welded at speeds of 70 in. per minute with argon gas and 186 in. per minute with Linde's argon sigma type gas.

This new type gas produces increased burn-off rate for a given amperage and it has been shown by high-speed photography that the metal transfer from the electrode is by many small globules, similar to a spraying action rather than the larger globule transfer coincident with argon gas. With a $^1/_{16}$-in. electrode on 0.064 aluminum alloys, approximately 1 lb. of rod is deposited per 100 ft. of lineal butt weld. The specimens welded were panels 3 x 30 in. in various thicknesses from 0.025 to 0.125 in. as shown in Fig. 3. The sigma-type gas produces a black deposit which must be removed prior to subsequent processing or for reasons of appearance.

The 302$^1/_2$H stainless steel in 0.025-in. gage was welded at 250 in. per minute using sigma-type gas. It is significant that carbide precipitation occurred, even at this high speed, in this type of unstabilized steel, whereas, precipitation did not occur when type 304 was welded. Fig. 4 shows the weld nugget and the relative position of the carbide precipitation in type 302

Table 2-Conditions of Welding and Physical Weld Tests

Fig. No.	*Alloy*	*Ultimate tensile strength, lb.*	*% Elong.*	*Location of failure*	*Welding speed, in./min.*	*Shielding gas*	*Material gage*	*Welding Amp.*	*Welding V.*
				Consumable electrode welding					
5	61ST-4	33,300	4.7	Edge of weld	70	Argon	0.064	130	20
11	61ST-4	36,100	7.5	Edge of weld	186	Sigma	0.064	170	22
7	61ST-6	33,300	3.6	Parent metal	68	Argon	0.064	130	20
8	61ST-6	31,175	2.6	Weld	186	Sigma	0.064	170	22
9	302$^1/_2$ H	119,900	4.8	Edge of weld	250	Sigma	0.025	180	22
				Tungsten electrode welding					
	61ST-4	33,600	4.5	Parent metal	10	Argon	0.064		
				Parent metal					
	61ST-4	41,100	19.6				0.064		
	61ST-6	46,000	11.6				0.065		
	302$^1/_2$ H	145,600	22.7				0.025		

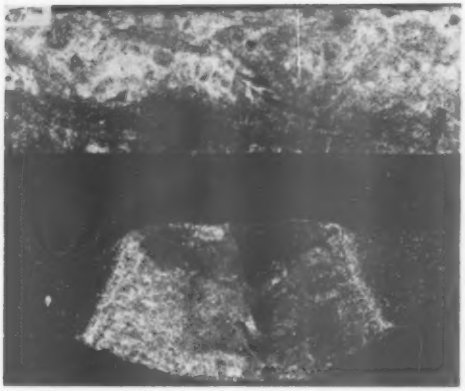

Fig. 6 Cross section and longitudinal section of aluminum alloy 61ST-4 welded by the consumable electrode machine

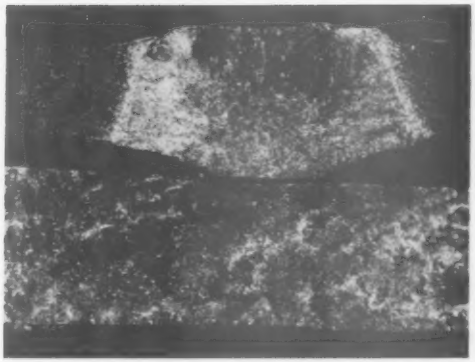

Fig. 8 Cross section and longitudinal section of aluminum alloy 61ST-6 welded by the consumable electrode machine

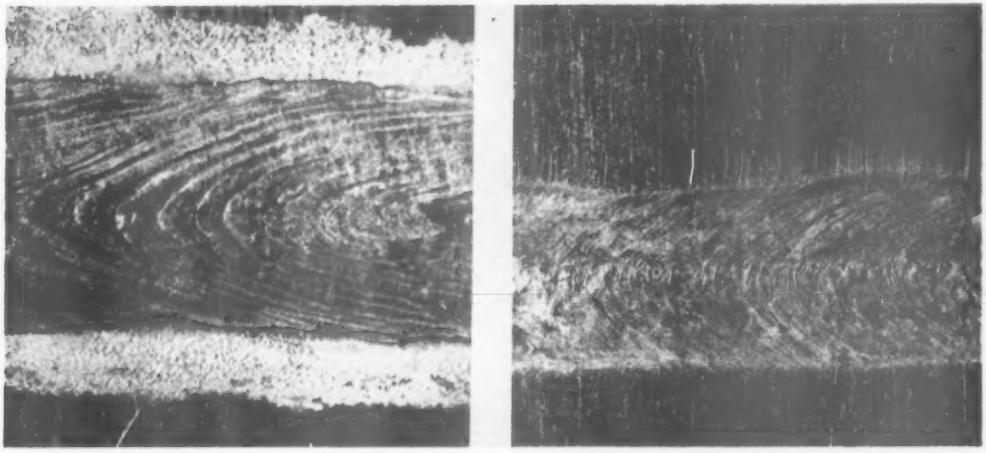

Fig. 7 External appearance of bead and underside of aluminum alloy 61ST-6 welded by the consumable electrode machine

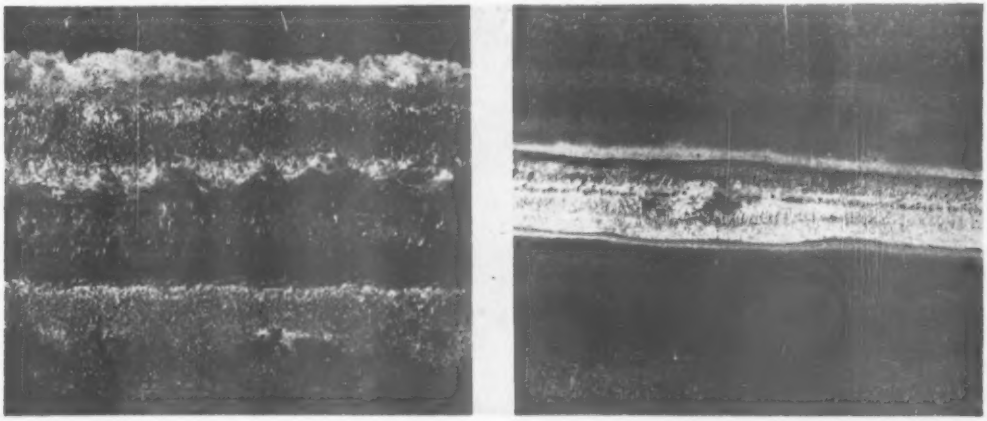

Fig. 9 External appearance of bead and underside of stainless steel 302½ H welded by the consumable electrode machine

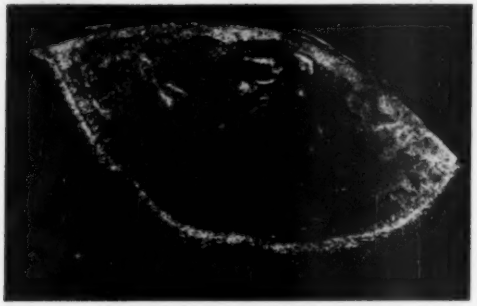

Fig. 10 Cross section of aluminum alloy 61ST-4. Material gage 0.090—lap weld

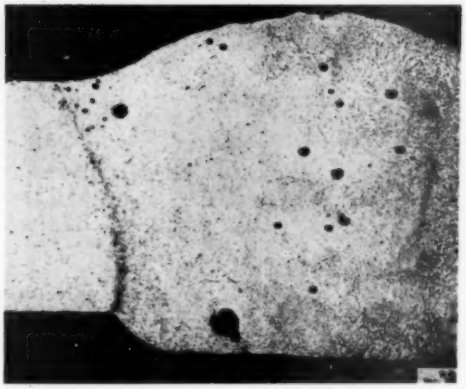

Fig. 11 Cross section of aluminum alloy 61ST-4. Typical micro-porosity found in beads welded by the consumable electrode machine at higher ranges of speed—186 in. per minute

stainless steel. In Fig. 4A, we see a cross section of type 304 stainless steel in 0.025 in. gage welded without any subsequent carbide precipitation. The short heat dwell that results from high speed welding and the low carbon content of type 304 make possible the welding of unstabilized stainless steel in light gages without accompanying precipitation of carbides. For corrosion-resistance applications of thin gage material, where annealing subsequent to welding is impractical, the use of the 304 type is, therefore, indicated.

The higher welding speed allows less heat conduction and therefore less subsequent distortion.

A copper back-up bar was used in all of the welding. Welding wire, $^{5}/_{8}$ in., 5% silicon was used for the aluminum alloys and Type 347 wire for the stainless. Temperature measurements taken $^{5}/_{8}$ in. from the weld revealed a maximum of only 150° F. when welding the aluminum alloys at a speed of 70 in. per minute.

Figure 5 shows the external appearance of bead and underside of aluminum alloy 61ST-4 welded by the consumable electrode type of machine. Figure 6 pictures the cross section and longitudinal section of aluminum alloy 61ST-4 welded by the consumable

Fig. 12 Cross section of aluminum alloy 61ST-4. Consumable electrode type welding practically free of micro-porosity and welded at 66 in. per minute

electrode machine. Figure 7 is the external appearance of bead and underside of aluminum alloy 61ST-6 welded by the consumable electrode machine. Figure 8 pictures the cross section and longitudinal section of aluminum alloy 61ST-6 welded by the consumable electrode machine. Figure 9 shows the external appearance of bead and underside of stainless steel $302^{1}/_{2}$ H welded by the consumable electrode machine. Figure 10 is a cross section of aluminum alloy 61ST-4, material gage 0.090, lap weld. Figure 11 shows a cross section of aluminum alloy 61ST-4—typical micro-porosity found in beads welded by the consumable electrode machine at higher ranges of speed (186 in. per minute). Figure 12 shows a cross section of aluminum alloy 61ST-4, consumable electrode type welding, practically free of micro-porosity and welded at 66 in. per minute. This amount of micro-porosity is considered about the lowest level obtainable during these tests. It is not surprising that welding at these speeds does not allow the complete escape of entrapped gases. However, with good welding conditions, porosity can be minimized to a level of acceptable quality for many applications. Table 2 gives the tensile properties with some of the welding conditions and a comparison with a machine tungsten electrode weld.

The tensile properties of the consumable electrode welds were comparable to those of the automatic welds obtained with the tungsten electrode and were superior to manual welding.

This evaluation indicates that high speed consumable electrode machine welding is a rapid joining process which can be used to advantage for aircraft.

Table 2 gives the tensile properties with some of the different welding conditions.

The Arc Welding of Carbon-Molybdenum Steel Pipe

Effects of preheating and stress relieving on the properties of welds in carbon—0.5 molybdenum steel pipe with recommendation where preheating and stress relieving may be eliminated

by F. J. Winsor

ABSTRACT

The effects of preheating and/or stress relieving on the properties of welds in carbon–0.5% molybdenum steel pipe ($^9/_{16}$-in. wall), furnace tubing ($^{11}/_{16}$-in. wall) and flange stock ($^3/_4$ in. thick) were investigated.

It is concluded that, for service temperatures above 800° F., preheating and stress relieving should not be required for welds made with cellulose-coated, Class E7010, electrodes in A206, A161, A234 and A182 carbon-molybdenum steels in thicknesses up to at least $^3/_4$ in. For welds made with manganese-molybdenum low-hydrogen electrodes, in the same range of thickness in these materials, preheating and stress relieving should not be required regardless of the service temperature.

The free-bend test, required by the various codes for qualification of welding procedure, was found to be of questionable value as an indicator of the serviceability of welded joints. This test rejected high-strength weld metal and passed low-strength weld metal without regard to the intrinsic ductility of each.

INTRODUCTION

The cost of stress relieving welded piping varies greatly depending mainly upon whether a large number of components can be stress relieved simultaneously in a furnace or whether heating of each weld must be performed individually because of the size of the weldment or because of a field location. Where individual heating of each weld is required, the cost of stress relieving may far exceed the cost of making the weld. With greater emphasis being placed upon efficient utilization of labor it is essential to know whether stress relieving is necessary and indispensable.

This investigation was undertaken to determine whether preheating and stress relieving could be eliminated in arc welding carbon–0.50% molybdenum steel piping materials without seriously affecting the serviceability of the welded joints. Since the requirements of various codes, which are applicable to refinery construction, concerning stress relieving of carbon-molybdenum steel, Table 1, differ, it might be inferred that these differences are based upon differences in service conditions. However, the A.S.A. Piping Code specifically applies to power piping as well as to oil, gas and air piping so it is difficult to understand the necessity for the stricter requirements of the A.S.M.E. Power Boiler Code.

The benefits of preheating and stress relieving have been evaluated here principally upon the basis of their effects on the strength and ductility of the welds. A few measurements were made to determine the residual stresses in stress-relieved and nonstress-relieved welds, but the practical application of the results, which must be considered as tentative, was difficult to visualize. No attention was paid, either, to any possible effects of preheating and stress relieving upon the tendency for graphitization although widely different results on the effects of postweld heating on the subsequent tendency for graphitization have been reported.[1]

Carbon-molybdenum steel is a high-temperature ma-

Table 1—Code Requirements Concerning Stress Relieving of Carbon-Molybdenum Steel

Code	*Where stress relief is required*
A.S.M.E. Power Boiler Code (Par. P-108)	All fusion-welded pressure parts of power boilers except C-Mo (0.20% max. C) piping $^6/_8$ in. thick and less
A.S.M.E. Unfired Pressure Vessel Code (Section VIII), Rev. 1950, Par. UW-10	Vessels constructed of SA-204 (carbon–0.5% molybdenum) plates when the thickness at any welded joint exceeds 0.58 in.
A.P.I.-A.S.M.E. Code for Unfired Pressure Vessels for Petroleum Liquids and Gases (1943 Ed.), Par. W-318-1, W-462, Appendix 6	When nominal wall thickness is $^1/_2$ in. or greater
A.S.A. Code for Pressure Piping A.S.A. B31.1-1942, Par. 628	When nominal wall thickness is $^1/_2$ in. or greater

F. J. Winsor was associated with the Materials Division, Engineering Research Department, Standard Oil Company (Indiana) at the time this paper was prepared and is now connected with E. I. du Pont de Nemours and Co., Wilmington, Del.

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Table 2—Chemical Analysis of Materials

Element % by Weight	A.S.T.M. A206		A.S.T.M. A161		A.S.T.M. A182	
	Specified	Actual	Specified	Actual	Specified	Actual
C	0.10-0.20	0.18	0.10-0.20	0.15	0.35 max.	0.28
Mn	0.30-0.80	0.55	0.30-0.80	0.65	0.30-0.80	0.72
P	0.040 max.	0.011	0.040 max.	0.012	0.040 max.	0.015
S	0.050 max.	0.029	0.040 max.	0.022	0.05 max.	0.020
Si	0.10-0.50	0.24	0.10-0.50	0.16	0.20-0.50	0.25
Cr	....	0.26	....	0.19	...	0.08
Mo	0.45-0.65	0.57	0.45-0.65	0.55	0.40-0.60	0.51

terial which is not known to be subject to any phenomenon resulting in loss of ductility at elevated temperatures, other than graphitization. Therefore, if welds in this material display adequate ductility at room temperature the ductility should be more than adequate at normal service temperatures. Consequently, it seems unnecessarily restrictive, and perhaps economically wasteful, to require just as much ductility in room temperature tests of welds in this material as in other materials which are employed at atmospheric temperatures.

MATERIALS AND ANALYSES

THE material employed for this investigation was A.S.T.M. A206 molybdenum-steel pipe. 6⅝ in. O.D. by 9/16-in. wall; A.S.T.M. A161 carbon-molybdenum steel still tubes, 5 in. O.D. by 11/16-in. wall; and material 6 x 6 x ¾ in. thick taken from a billet from which A.S.T.M. A182-F1 carbon-molybdenum welding flanges are forged. Table 2 gives the chemical analyses of these materials.

For most of the tests, A.W.S. Class E7010 electrodes were employed. Tests also were made with low-hydrogen electrodes, A.W.S. Classes E7015, E8015-16, E9016, E10016 and E12015, obtained from various manufacturers. Typical chemical compositions of the weld metal deposited by these electrodes are given in Table 3.

BEND TESTS OF E7010 WELDS IN A206 PIPE

In his work on the effects of cooling rate on the properties of arc-welded joints in carbon-molybdenum steel, Hess[2] reported that tensile tests of welds made in ¾-in. thick plate without preheating or stress relieving showed evidence of weld metal embrittlement. Tensile fractures of the welds revealed the presence of "fisheyes." In view of the reported effect of hydrogen embrittlement on lowering the tensile ductility it was assumed that a corresponding effect would be evident in the results of bend tests. Therefore, a series of butt welds was made in 6⅝ in. O.D. by 9/16-in. wall A206 pipe using various welding conditions, as outlined in Table 4. The joint detail for these welds comprised a 75-degree included angle, 1/32-in. root face and 3/32-in. root opening (the root opening was increased to 3/16 in. for weld No. 14 in which a backing ring was used).

After welding, and heat treatment where specified, the pipe was sectioned to produce test specimens. Two face-bend, two root-bend and two to four free-bend tests were made on each weld specimen. It was originally intended to run the guided-bend tests in the full 9/16 in. thickness of material with the thought that the greater severity of the tests would provide greater sensitivity to weld metal embrittlement. However, in the first group of tests which were made it was found that none of the root-bend specimens was able to pass regardless of whether preheating or stress relieving had been employed. Therefore, subsequent face and root-bend tests were made on specimens which had had their surfaces machined parallel and the thickness reduced to ⅜ in. by machining from the face of the weld. The results of the bend tests are shown in Table 4.

Of the 28 face-bend specimens tested, only three failed (one from each of weld Nos. 2, 7 and 11). All of these welds were made with the pipe axis in the vertical, fixed position. Weld No. 2 was made without preheat or stress relief; No. 7 was not preheated, but was stress relieved; and No. 11 was preheated but not stress relieved. All of the failures in this test were clearly due to the presence of porosity or slag inclusions.

Of the 28 root-bend specimens, only four passed (one from weld No. 5, one from No. 13, and two from No. 14).

Table 3—Typical Chemical Compositions of Weld Metal Employed in This Investigation

Electrode Class	Carbon, %	Manganese, %	Silicon, %	Molybdenum, %	Chromium, %	Vanadium, %
E7010-A*	0.07	0.34	0.15	0.52	...	...
E7010-B	0.07-0.10	0.45-0.60	0.10-0.20	0.50-0.60	...	...
E7015	0.08	1.25	0.40	...	...	...
E8015-A	0.12	0.68	0.22	0.45	0.90	...
E8015-B	0.10	0.80	0.41	0.40	0.42	...
E8016-C	0.12	0.46	0.15	1.35	...	0.16
E9016	0.10	0.70	0.60	0.59	...	...
E10016	0.16	1.94	0.33	0.33	...	...
E12015	0.10	1.80	...	0.35	...	...

* Letters refer to different manufacturers.

Table 4—Conditions Used for Preparation of Butt-Weld Specimens in A206 Carbon-molybdenum Pipe and Results of Bend Tests*

(6⅝ in. O.D. by ⅝-in. wall; 37½° bevel; no backing ring except where mentioned; E7010 electrodes)

Weld No.	Electrode diameter, in.	Welding current, amp.	Position of pipe axis	Preheat	Stress relief	No. of specimens passing or failing test: Face-bend	Root-bend	Free-bend	Average Elongation in free-bend tests
1	1/8	95	Horizontal, fixed	None	None	2P, 0F	0P, 2F	2P, 0F	34
2	1/8	85	Vertical, fixed	None	None	1P, 1F	0P, 2F	1P, 0F	25
3	5/32	130	Horizontal, fixed	None	None	2P, 0F	0P, 2F	2P, 0F	42.5
4	5/32	120	Vertical, fixed	None	None	2P, 0F	0P, 2F	2P, 0F	45
5†	3/16	150	Horizontal, rolled	None	None	2P, 0F	1P, 1F	3P, 0F	37
6	5/32	130	Horizontal, fixed	None	1200° F., 1 hr.	2P, 0F	0P, 2F	2P, 0F	37
7	5/32	120	Vertical, fixed	None	1200° F., 1 hr.	1P, 1F	0P, 2F	2P, 0F	43
8	1/8	95	Horizontal, fixed	400° F.	None	2P, 0F	0P, 2F	2P, 0F	33.5
9	1/8	85	Vertical, fixed	Minimum	None	2P, 0F	0P, 2F	3P, 0F	42
10†	5/32	130	Horizontal, fixed	400° F.	None	2P, 0F	0P, 2F	2P, 0F	33
11	5/32	120	Vertical, fixed	Minimum	None	1P, 1F	0P, 2F	3P, 0F	43
12†	5/32	130	Horizontal, fixed	400° F.	1200° F., 1 hr.	2P, 0F	0P, 2F	4P, 0F	45
13†	5/32	120	Vertical, fixed	400° F.	1200° F., 1 hr.	2P, 0F	1P, 1F	4P, 0F	47.5
14‡	1/8	95	Horizontal, fixed	None	None	2P, 0F	2P, 0F	4P, 0F	29.5

* Test results were evaluated in accordance with the requirements of paragraphs Q-109 (b) and (c), A.S.M.E. Boiler Construction Code, Section IX, Standard Qualification for Welding Procedure and Welding Operator.
† Machined to ¾ in. thickness prior to testing.
‡ Same as No. 1, except backing ring used.

All of the specimens which passed had been machined to ¾ in. thickness rather than tested in the full wall thickness of the pipe. Weld No. 5 was a horizontal roll weld made with a larger size electrode than any of the other welds. No. 13 was made with 400° F. preheat and 1200° F. stress relief. Weld No. 14 was made with small electrodes and neither preheated nor stress relieved, but a backing ring was employed. In general, the root-bend tests of the stress-relieved welds gave evidence of greater ductility than similar tests of nonstress-relieved welds. From the results of later work it appeared that the root-bend failures of the stress-relieved welds were due mainly to the effect of a mechanical notch resulting from the presence of weld metal having lower strength than the parent metal in a V-type joint with a narrow root. With such a condition, bending may cause excessive straining and, eventually cracking, of the weld metal at the root, even in the absence of defects.

The favorable results of the root-bend tests of weld No. 14, made without preheating or stress relieving and using a backing ring, apparently support this explanation. The use of the backing ring permitted increasing the width of the weld at the root, and, since the weld was not stress relieved, the strength of the weld metal was higher in relation to the strength of the parent metal. Both of these factors tended to assist passage of the root-bend test even though the weld metal must have been inherently less ductile than stress-relieved weld metal would have been. Thus, there is the rather anomalous situation of failure occurring in the root-bend test, of an inherently ductile weld metal (one which had been stress relieved) subjected to excessive strain due to combined effects of low strength and joint design, whereas, a less ductile, and possibly brittle, weld metal passed the test because conditions were such that it was subjected to less strain.

From these results it appears that, under certain conditions, the root-bend test may be somewhat confusing as a criterion of weld metal quality, and there may be reason for casting suspicion on the acceptability of welded joints in which the strength of the weld metal is somewhat less than the actual strength of the parent metal, even though the weld metal ductility is high and its strength is considerably above the minimum strength specified for the parent metal.

All of the free-bend tests passed the requirements of the Boiler Code of 30% elongation for stress-relieved welds and 25% elongation for nonstress-relieved welds. From measurements of elongation in the free-bend test there was no indication of superiority of the stress-relieved welds over the nonstress-relieved welds and also no indication of superiority of welds made with preheat over those made without preheat. However, there was a rather decided improvement in free-bend ductility with increase in the size of electrode, particularly where preheating was not employed. With the exception of the series of welds made with ⅛-in. diameter electrodes, without preheat or stress relief (weld Nos. 1 and 2), welds made with the pipe axis vertical had somewhat higher elongations in the free-bend test than welds made with the pipe axis horizontal.

HARDNESS TESTS OF E7010 WELDS IN A206 PIPE

Hardness traverses were made on transverse sections taken from welds numbered 3, 10, 6 and 12, which were selected on the basis of constant electrode size (5/32 in.) and various combinations of preheating and stress relieving. The measurements were made on a Tukon hardness tester using a diamond pyramid indenter, 1 kg. load, and a spacing of 0.2 mm. between inpressions. The hardness traverse was oriented parallel to the top surface of the section and was made as near to the surface as possible since the highest hardnesses usually are found in the last pass of a butt weld.

Table 5 gives values of the maximum hardness mea-

Table 5—Weld and Heat-Affected Zone Hardnesses of Welds in A206 Pipe

All welds and heat treatments (except stress relief of No. 6) were made in the shop (5/32 in. diameter E7010 electrodes)

Weld no.	Conditions	—Diamond pyramid hardness— 1 kg. load: Maximum weld hardness	Heat-affected Zone hardness: max.	Heat-affected Zone hardness: av.
3	No preheat; no stress relief	214	225	215
10	400° F. preheat; no stress relief	198	231	218
6	No preheat; 1200° F., 1 hr. stress relief	205	200	189
12	400° F. preheat; 1200° F.1 hr. stress relief	185	217	194

NOTE: Parent metal hardness was 160.

sured in the weld metal and the maximum and average hardnesses in the heat-affected metal. The data indicate that preheating alone (weld No. 10) had little effect on the hardness, but the effect of stress relieving alone (weld No. 6) was somewhat greater in the heat-affected zone. Preheating and stress relieving together (weld No. 12) lowered the hardness both in the weld metal and in the heat-affected metal, but the difference was hardly enough to indicate clearly the necessity for preheating and stress relieving.

A question might be raised as to why weld No. 10, made with preheat, differed so little in hardness from weld No. 3 on which no preheating was employed. In manual arc welding of pipe, except in thick-walled material, most welds are self-preheated. That is, in continuous welding, the heat lost to the material in depositing the first pass or two of weld metal raises the temperature at the joint to well above the preheat temperature of 400° F. used for this series of welds. In test welding under shop conditions no effort was made to prevent the interpass temperature from building up.

IMPACT TESTS OF E7010 WELDS IN A206 PIPE

In order to obtain further information on the properties of welds made with and without preheating and stress relieving, impact tests were run at temperatures ranging from +76 to −98° F. Charpy keyhole specimens, taken transverse to the welds and notched from the face, were employed. The results were plotted to give the energy absorption vs. temperature curves shown in Fig. 1.

A comparison of the impact test results has been made by selecting, from the curves in Fig. 1, temperatures corresponding to an energy absorption of 15 ft.-lb., which usually is considered to represent a minimum satisfactory value for materials which are notch sensitive.

These temperatures, Table 6, indicate that there is: (*a*) practically no change in the "transition" temperature of the parent metal as a result of 400° F. preheating and 1200° F., 1 hr. stress relieving; (*b*) a 10° lower

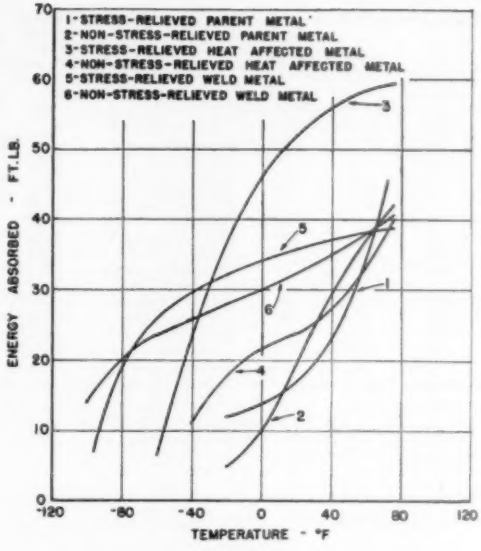

Figure 1

transition temperature for the nonstress-relieved weld metal than for the stress-relieved weld metal; and (*c*) a 21° lower transition temperature for the heat-affected zone in the stress-relieved weld than for the corresponding zone in the nonstress-relieved weld. Because of the small number of specimens tested, the curves for the weld metal are not sufficiently well defined to attach much significance to a temperature difference of only 10°, but the heat-affected zone difference is larger and is accompanied by a higher energy absorption in the ductile range, so this is felt to be significant. However, in view of the fact that the transition temperature of the parent metal is much higher than those either of the weld metal or of the heat-affected metal, the improvement in impact properties of the heat-affected metal brought about by stress relieving does not appear to be of any practical importance.

IMPACT TESTS OF E7010 WELDS IN A161 TUBING

Impact tests were made also of stress-relieved and nonstress-relieved E7010 weld metal at temperatures

Table 6—Temperatures Corresponding to Minimum Energy Absorption of 15 Ft.-Lb. in Charpy Tests of E7010 Welds in A206 Pipe

Welding conditions	Structure	Temperature (° F.) below which energy absorption is less than 15 ft.-lb.
No preheat; no stress relief	Parent metal	+12
	Weld metal	−97
	Heat-affected metal	−30
400° F. preheat; 1200° F., 1 hr. stress relief	Parent metal	+9
	Weld metal	−87
	Heat-affected metal	−51

ranging from +75 to +220° F. Welds were made in 5 in. O.D. by $^{11}/_{16}$-in. wall, A161 tubing. One weld was made without preheat or stress-relieving; the maximum interpass temperature was 200° F. The other weld was made with 400° F. preheat and 1200° F., 1 hr. stress-relief. Charpy keyhole specimens were used, the notch being located in the weld metal perpendicular to the face of the weld.

There was no consistent trend of energy absorption vs. temperature for either weld. At temperatures between 75 and 220° F. the average energy absorption for the nonstress-relieved weld was about 41 ft.-lb., with a maximum of 63 ft.-lb. at 121° F. and a minimum of 22 ft.-lb. at 173° F. Over the same temperature range, the average energy absorption for the stress-relieved weld was 60 ft.-lb., with a maximum of 74 ft.-lb. at 121° F. and a minimum of 50 ft.-lb. at 75° F. These data indicate a somewhat higher capacity for energy absorption for E7010 weld metal in the stress-relieved condition than in the as-welded condition at temperatures ranging from 75 to 220° F.

PROPERTIES OF E7010 WELDS IN A161 TUBING

Although the previous work involving guided root- and face-bend tests, free-bend tests, impact tests and hardness measurements had failed to indicate a substantial lack of ductility in nonstress-relieved welds, there still was a suspicion that such lack of ductility must exist, undetected by the tests which had been employed, if the results of Hess[2] and other investigators were valid. Consequently, further studies were made of welds in A161 carbon-molybdenum furnace tubing in which transverse tensile tests were added, and side-bend tests were substituted for the face- and root-bend tests which had been made previously. In addition, higher preheating and stress-relieving temperatures were employed for some of the welds in an effort to determine whether such conditions might produce a greater difference between welds made with and without preheating and stress-relieving.

The preheating and stress-relieving conditions shown in Table 7 were used in making butt welds with $^{5}/_{32}$-in. E7010 electrodes in 5 in. O.D. by $^{11}/_{16}$-in. wall A161 tubing. Each weld was cut up to produce two reduced-section tensile test bars, two free-bend test bars and four side-bend test bars.

TENSILE TESTS OF E7010 WELDS IN A161 TUBING

The results of the tensile tests are shown in Table 7. Weld No. 2, made with 700° F. preheat and no stress relief, had the highest tensile strength, but the weld metal elongations were appreciably lower than those of stress-relieved welds. Both the strength and the ductility of weld No. 5, made without preheat or stress relief, were lowered by the presence of hydrogen. Although no tests were made on the parent metal, an approximation of its strength can be gained from the fact that one of the specimens from weld No. 2 failed in the parent metal at a strength of 71,400 psi. The average of the strengths for weld No. 4 (68,150 psi.), made without preheat but stress-relieved at 1200° F., was about 5% less than the strength of specimen No. 2, which was preheated to 700° F. before welding, but not stress-relieved. Stress-relieving at 1275° F., in the case of welds Nos. 1 and 3, lowered the strength even more.

It should be mentioned that the results of all of the tensile tests satisfy the requirement of paragraph Q-109 (a), Section IX, of the A.S.M.E. Boiler Code for a tensile strength not less than 100% of the minimum of the specified tensile range of the base material (in this case 55,000 psi.). There is no minimum requirement for elongation in the reduced-section tensile test.

BEND TESTS OF E7010 WELDS IN A161 TUBING

Examination of the data in Table 7 for the free-bend tests shows that the embrittlement in the tensile test may not be reflected in the free-bend test, although the latter is supposed to provide a measure of the weld metal ductility. The highest elongations across the weld and also in the heat-affected zones were obtained on weld No. 5, the tensile test bars of which gave the

Table 7—Results of Tests of E7010 Welds in 5 In. O.D. by $^{11}/_{16}$-In. Wall A161 Tubing

		Transverse tensile tests			*Free-bend tests*		
Weld No.	*Conditions*	*Ultimate strength, psi.*	*Elongation across weld, %*	*Elongation in 1.75 in., %*	*Elongation across weld, %*	*Average elongation outside weld, %*	*Side-bend tests*
1	700° F. preheat; 1275° F., 1 hr. stress relief	65,300	44.0	31.4	61.4	24.3	4P
		63,900	33.5	28.0	52.9	23.0	
2	700° F. preheat; no stress relief	72,100	28.0	22.3	62.6	23.5	3P, 1F*
		71,400	19.5	41.1	57.4	21.0	
3	No preheat; 1275° F., 1 hr. stress relief	65,100	39.0	31.4	60.3	29.7	4P
		65,700	38.0	30.3	53.3	21.2	
4	No preheat; 1200° F., 1 hr. stress relief	69,200	49.0	36.0	56.0	18.8	4P
		67,100	30.0	23.4	54.5	31.2	
5	No preheat; no stress relief	67,900	29.0	18.9	57.8	31.4	2P, 2F*
		66,400	19.5	13.1	70.0	22.6	

* Side-bend failures occurred straight through the weld metal and did not follow the fusion line. All tensile fractures occurred through the weld metal with the exception of one bar from weld No. 2, which failed in the parent metal. The tensile fractures from weld No. 5 showed "fisheyes," characteristic of hydrogen embrittlement.

lowest ductility. These results are in line with the results obtained previously on free-bend tests of welds in A206 pipe.

The results of the side-bend tests indicate rather definite superiority of the stress-relieved welds. However, the side-bend test may not be a completely dependable indicator of hydrogen embrittlement as shown by the fact that two of the specimens from weld No. 5 passed the side-bend test. It appears reasonable to conclude from these tests that stress-relieving is definitely effective in eliminating hydrogen embrittlement, whereas preheating may be only partially effective. Comparing preheat temperatures, it would be expected that 700° F. preheating would be more effective than 400° F. preheating.

DISCUSSION OF HYDROGEN EMBRITTLEMENT OF WELD DEPOSITS

As a result of the tests on A161 tubing it appears probable that hydrogen embrittlement is mainly responsible for the inferior tensile properties of welds in this material which were tested in the as-welded condition. It becomes important to consider the effects of such embrittlement on the serviceability of welded structures.

It is interesting to note that hydrogen embrittlement was not detected in the impact tests below room temperature of E7010 weld metal which had been taken from a weld made in $^{9}/_{16}$-in. wall A206 pipe, without preheat or stress-relief. However, there was some indication of embrittlement in the root-bend tests of nonstress-relieved welds, beyond that caused by root defects in the welds. There was a definite indication of embrittlement in the higher temperature impact tests, the transverse tensile tests and the side-bend tests of nonstress-relieved welds in $^{11}/_{16}$-in. wall A161 tubing. These results indicate that hydrogen embrittlement in weld deposits is dependent upon the rate and temperature of straining. Herres[3] has shown that the rate and temperature of straining are factors in the embrittlement of cathodically charged steel. His explanation for the dependence of hydrogen embrittlement on rate of loading and temperature is that it is caused by a precipitation reaction during straining and that the reaction is limited by rate of diffusion of hydrogen.

It is noteworthy that positive evidence of hydrogen embrittlement (the presence of "fisheyes" in the tensile fractures) might not have been detected in the transverse tensile tests had the strength of the weld metal been somewhat higher. Straining of the weld metal is a necessary prerequisite for hydrogen embrittlement. Consequently, it is expected that limiting the amount of strain forced on the weld metal (by increasing the strength of the weld metal in a transverse tensile test) would affect the apparent embrittlement due to hydrogen. A similar effect might be produced in the side-bend test. In cases where the weld metal is definitely stronger than the parent metal, other tests would have to be employed to detect hydrogen embrittlement, such as longitudinal tensile tests or, possibly, notched slow-bend tests.

Zappfe and Sims[4] have reported that the embrittlement of weld deposits due to hydrogen absorption "is always removed and the tensile ductility of the weld metal improved by low-temperature (200–1200° F.) reheating of the weld metal for appropriate times or even by aging for several days at normal temperature." Also, Flanigan[5] reports a considerable increase in ductility of weld deposits as a result of heating for 3 hr. at 650° F. Consequently, there would appear to be no hazard, due to hydrogen embrittlement, in the use of carbon-molybdenum welds in the as-welded condition at elevated temperatures.

EFFECT OF LOW TEMPERATURE POSTHEAT TREATMENTS ON THE STRENGTH AND DUCTILITY OF WELDS IN A161 TUBING

Butt welds were made with E7010 electrodes in 5 in. O.D. by $^{11}/_{16}$-in. wall A161 tubing. Preheating was not employed during welding, and the interpass temperature was kept below 200° F. Exposure times of 24 and 96 hr. at temperatures of 400 and 800° F. were employed for the postheat treatment. After heat treatment the welds were cut up for reduced-section tensile specimens and side-bend specimens. The results of these tests are shown in Table 8.

It is interesting that, in this series of tests, none of the bend test or tensile test failures in the welds showed the presence of "fisheyes"; however, one of the side-bend specimens in each of the as-welded, 400° F.-24 hr. and 400° F.-96 hr. series failed completely through the weld, indicating weld metal embrittlement. None of the 800° F. specimens failed in this manner. Neither the elongation measurements nor the location of failure in the tensile specimens provide a clear indication of the effectiveness of the postheat treatments in reducing embrittlement. The failure of one of the 400° F.-96 hr. specimens, which occurred in the weld, could not be attributed to the presence of slag inclusions, as in other cases of weld metal failures; therefore, this probably is an indication of hydrogen embrittlement.

Comparing the 800° F.-96 hr. treatment with the 1200° F.-1 hr. treatment, it may be seen that the total elongation in the tensile test was slightly higher and the strength was slightly lower for the 1200° F. treatment. The weld metal elongation was considerably higher for the 1200° F. stress-relieved weld because failure occurred in the weld rather than in the parent metal. From these tests it appears that a service temperature of 800° F. could be relied upon to improve the ductility of the joints to an entirely satisfactory value.

SIDE-BEND TESTS IN A161 TUBING WELDED WITH LOW-HYDROGEN ELECTRODES

Series of welds were made in $^{11}/_{16}$-in. wall A161 tubing using each of the following electrodes: E10016, E8016-C, E12015, E8015-A and E8015-B. Typical

Table 8—Effect of Low-Temperature Postheat Treatments on E7010 Welds in A161 Tubing

Postheat temperature, ° F.	Time at temperature, hr.	Side-bend test results	Reduced-section tensile tests: Ultimate strength, psi.	Reduced-section tensile tests: % Elongation in weld	Reduced-section tensile tests: % Elongation total	Reduced-section tensile tests: Location of failure
As-Welded		Failed; crack propagated through weld from unfused root	72,200	23.2	39.8	Outside of weld
As-Welded		Passed; small crack at unfused root	73,300	11.4	23.1	Outside of weld
400	24	Passed; small crack at unfused root	72,100	17.9	31.4	Outside of weld
400	24	Failed; crack propagated through weld from unfused root	70,500	35.8	22.1	In weld, slag inclusions
400	96	Failed; crack propagated through weld from unfused root	69,000	34.3	20.2	In weld
400	96	Passed; small crack near face	71,000	24.7	19.0	In weld, inclusions
800	24	Passed; no defects	72,000	10.5	20.8	Outside of weld
800	24	Failed; 3/32 in. defect near fusion line	71,800	9.1	22.9	Outside of weld
800	96	Passed; very small crack at fusion line	73,100	9.6	24.4	Outside of weld
800	96	Passed; no defects	71,200	11.0	28.9	Outside of weld
1200*	1	Passed	68,150	39.5	29.7	In weld

* From Table VII (average of the values shown for weld No. 4)

chemical analyses of the weld metal deposited by these electrodes are shown in Table 3. Unfortunately, none of these electrodes produces a carbon-molybdenum weld metal having a chemical composition similar to that produced by the ordinary E7010 electrodes. However, they should all produce weld metal having strengths at elevated temperatures equaling or exceeding that of E7010 weld metal. The low-hydrogen welds were made without preheating or stress relieving, and the interpass temperature was kept below 200° F. The welds were subjected to side-bend tests only. It was considered valueless to employ reduced-section tensile tests since all of these electrodes (in contrast with Class E7010) produce weld metal of higher strength than the A161 parent metal, and failure in this test would occur in the parent metal unless the weld were extremely defective.

The side-bend test results were as follows:

1. E10016: 3 passed, none failed.
2. E8016-C: 1 passed, 2 failed. (One failure was due to propagation of a crack from the unfused root; the other showed slight porosity.)
3. E12015: 3 passed, none failed.
4. E8015-A: 2 passed, 1 failed. (Failure due to propagation of a crack from the unfused root.)
5. E8015-B: 1 passed, 2 failed. (Both failures were due to propagation of cracks from the unfused root.)

With respect to the higher strength weld metals (E10016 and E12015) these results indicate either that it is easier to obtain good root penetration with these electrodes, or, more likely, that, even if an unfused root condition exists, the higher strength of the weld metal forces elongation to occur outside of the weld, thus preventing propagation of a root crack. Of course, this resistance of the weld to deformation may result in inability to comply with the requirements of the free-bend test. In specimens from the other welds, in which failure occurred due to propagation of root cracks, it is difficult to say whether hydrogen embrittlement was a factor inasmuch as all of the specimens displayed considerable ductility, with the exception of the one specimen from the weld which had considerable porosity.

There may be some objection to the use of the side-bend test to evaluate the quality of a weld in which the strength of the weld metal is considerably greater than the strength of the parent metal because the elongation will be forced to take place preferentially in the parent metal. Because of this, the presence of low ductility in a high-strength weld metal might easily escape detection in the side-bend test. In order to obtain assurance that the high-strength, low-hydrogen weld metals previously studied had adequate ductility, side-bend tests were run on all-weld metal specimens. Such specimens, 3/4 in. thick, were made with E10016, E8015-A and E7010 electrodes without preheating or stress relieving, and also with E7010 electrodes using 400° F. preheat and 1200° F., 1 hr., stress relief. Except for the preheated E7010 weld, the interpass temperature was kept below 200° F. during welding.

All of these specimens underwent complete bending without sign of cracking, indicating that, under conditions where it is possible to subject the weld metal to deformation, the higher-strength, low-hydrogen weld metal is fully as capable of withstanding at least the 20% average elongation imposed by the side-bend test as is the lower strength weld metal made with cellulose-coated electrodes. It is interesting that in the all-weld metal side-bend test there was no indication of embrittlement in the E7010 weld made without preheat or stress-relief, although previous experience with normal side-bend tests of butt welds in pipe would have presaged such embrittlement. This experience with the all-weld-metal side-bend test suggests, perhaps, that, in bending, more than 20% strain is required in order to produce hydrogen embrittlement. In side-bend tests of single-V butt welds in which the strength of the weld

metal is less than the strength of the parent metal, or in which there is a small notch due to slag or incomplete penetration at the root, the local strain at the root could easily reach a very high value with only a small amount of bending, thus promoting hydrogen embrittlement and progressive failure.

FREE-BEND TESTS IN A206 PIPE WELDED WITH LOW-HYDROGEN ELECTRODES

A comparison of the free-bend properties of welds made in $^{9}/_{16}$-in. wall A206 pipe with E7010 electrodes and with E10016 low-hydrogen electrodes is presented in Table 9.

Table 9. Comparison of Free-Bend Properties of E7010 Welds and E10016 Low-Hydrogen Welds in A206 Pipe

*Weld No.**	*Electrode type*	*Electrode diameter, in.*	*Position of pipe axis*	*Bend elongation, %*
1	E7010	$^{1}/_{8}$	Horizontal, fixed	34, 34
2	E7010	$^{1}/_{8}$	Vertical, fixed	25
3	E7010	$^{5}/_{32}$	Horizontal, fixed	43, 42
4	E7010	$^{5}/_{32}$	Vertical, fixed	36, 54
5	E7010	$^{3}/_{16}$	Horizontal, rolled	36, 38
15	E10016	$^{1}/_{8}$	Horizontal, fixed	21, 20, 27, 50
16	E10016	$^{1}/_{8}$	Vertical, fixed	22, 9, 12, 3
17	E10016	$^{5}/_{32}$	Horizontal, fixed	30, 9
18	E10016	$^{5}/_{32}$	Vertical, fixed	14, 27, 18, 14
19	E10016	$^{3}/_{16}$	Horizontal, rolled	18, 14, 20

* All welds were made without preheating or stress relieving.

As mentioned previously, the greater resistance to deformation of the E10016 weld metal, which probably assists passage of the side-bend test even in the presence of root defects, also limits the elongation of the weld metal in the free-bend test.

On the basis of these tests and previous experience with E7010 welds, which were known to be susceptible to hydrogen embrittlement, it is questionable whether the free-bend test is a valid measure of the ductility of welded joints In one case (nonstress-relieved E7010 weld metal), the test usually reveals no lack of ductility, although lack of ductility was shown to exist in tensile and side-bend tests. In the other case (E10016 weld metal) the test suggests lack of ductility, although all-weld metal side-bend tests revealed the ability of the weld metal to accept at least 20% elongation in bending. Unless high-strength weld metal is very definitely brittle it is difficult to understand why a welded joint containing such metal should be considered inferior to a joint containing lower strength, and also possibly less ductile, weld metal. It appears that the free-bend test for qualification of welded joints attaches a premium to the presence in a welded joint of weld metal of equal or lower strength than the parent metal. Higher strength weld metal is automatically ruled out regardless of its inherent ductility.

TESTS OF E9016 WELDS IN A161 TUBING

Two butt welds were made in the $^{11}/_{16}$-in. wall A161 tubing, using E9016 electrodes which had been specifically recommended for this sort of work. The tube sections were positioned so that welding proceeded vertically up. Before welding the electrodes were heated for at least 1 hr. at 400° F. to drive off absorbed moisture. Preheating was not employed, and the interpass temperature was kept below 200° F. during welding. One of the specimens was stress-relieved for 1 hr. at 1200° F. after welding. The specimens were sectioned to produce bars for reduced-section tensile tests, free-bend tests and side-bend tests.

In the reduced-section tensile test the ultimate strength of the nonstress-relieved weld was 72,300 psi., and that of the stress-relieved weld was 69,300 psi. Failure occurred in the parent metal in both cases.

The free-bend test results of the E9016 welds were rather unsatisfactory, both for the nonstress-relieved weld and for the stress-relieved weld. The nonstress-relieved weld fractured completely at a small bend angle, fracture occurring along the line of fusion. The fracture surface showed numerous small "fisheyes." The elongation in the weld was only 17.8%. The stress-relieved weld gave an elongation of 26.4%, but the surface showed numerous small checks.

The results of the side-bend test were unsatisfactory for the nonstress-relieved weld. The face of the specimen showed a $^{3}/_{32}$-in. root crack and a $^{1}/_{16}$-in. long check near the fusion line. The stress-relieved weld passed the side-bend test, but the surface showed numerous small checks at the fusion line.

The results of the tests with the E9016 electrodes, together with tests of other low-hydrogen electrodes, described previously, indicate that the electrodes which contained high manganese (over 1.00%) gave results superior to those obtained from the electrodes with low manganese. Thus, the weld quality in the as-welded condition appeared to be better for the E7015 (high-manganese, molybdenum-free) electrode, and for the E10016 and E12015 (manganese-molybdenum) electrodes, than for the E8015-A and E8015-B (chromium-molybdenum) electrodes, the E8016-C (molybdenum-vanadium) electrode and the E9016 (carbon-molybdenum) electrode.

It is somewhat disturbing that we have not yet been able to find a satisfactory 0.50% molybdenum, low-hydrogen electrode which is capable of passing all of the qualification tests in the as-welded condition. However, our experience indicates that the manganese-molybdenum electrodes would be completely satisfactory despite the unfavorable free-bend test results.

WELDING OF A182 CARBON-MOLYBDENUM FLANGE STOCK

A comparison of the chemical composition specifications for carbon-molybdenum piping components reveals that the maximum permissible carbon content for forged welding flanges is 0.35%, which is 0.15% greater than the maximum permissible carbon content for

A206 pipe. In general, the weldability of steel is inversely related to the carbon content; therefore, the latter is an important factor in considerations of the necessity for preheating and stress-relieving.

The flange stock material used for welding tests was in the form of $^3/_4$-in. thick blocks cut from a 6- x 6-in. carbon-molybdenum steel forging billet. The chemical composition of the material is shown in Table 2. The carbon content of this flange stock was 0.28%, as compared with 0.18% carbon for the A206 pipe, and 0.15% carbon for the A161 tubing used previously.

With the limited material available, six welds were made with various welding conditions, as shown in Table 10. After stress-relieving, where applied, the welds were cut into specimens for reduced-section tensile tests, side-bend tests and hardness surveys.

The test results, which are shown also in Table 10, indicate that the ductility of E7010 weld deposits was definitely improved either by stress-relieving at 1200° F. for 1 hr. or by postheating at 800° F. for 24 hr. (the latter to simulate service conditions). Preheating at 400° F., alone, did not improve ductility over that as-welded. The E7015 low-hydrogen electrode produced a weld without preheating or stress relieving which had greater strength and ductility than the weld made with E7010 electrodes employing preheat and stress-relief. However, this weld contains no molybdenum.

Analysis of the hardness measurements indicates that the weld metal hardness was little affected either by preheating alone or by preheating and stress-relieving. Stress-relieving at 1200° F. alone, and, also, postheating at 800° F. raised the hardness of the weld metal. Either preheating or stress-relieving, or both, lowered the hardnesses in the heat-affected metal. It appears that preheating was slightly less effective than stress-relieving in reducing the heat-affected zone hardness. Postheating at 800° F. had no effect on the heat-affected zone hardness. Hardnesses in the E7015 weld were about the same as in the E7010 welds, but hardnesses in the E7015 heat-affected zone were appreciably higher than those in the corresponding region of the non-preheated, nonstress-relieved E7010 weld. The higher hardnesses in the E7015 weld heat-affected zone could have been due either to segregations of carbon in the test plate or to the use of a lower energy input during welding.

The data indicate that hardness measurements are not necessarily acceptable criteria of the ductility of welded joints. Although the low-hydrogen weld showed considerably higher hardnesses in the heat-affected zone, it underwent slightly greater total elongation in the reduced-section tensile test. Conversely, the presence of hydrogen in weld metal may produce embrittlement under certain conditions of loading, and this embrittlement is not detected by hardness measurements.

As a result of these tests it is concluded that a stress-relieving treatment is unnecessary for E7010 welded joints in the higher-carbon, carbon-molybdenum flange steel up to $^3/_4$ in. thickness intended to operate at temperatures of 800° F. or higher. The service temperature can be relied upon safely to promote the diffusion of hydrogen out of the weld metal and so improve the ductility without sacrificing strength. The use of low-hydrogen electrodes is recommended when high ductility in the as-welded condition is required at service temperatures below 800° F.

MEASUREMENT OF RESIDUAL STRESSES IN WELDS

In addition to investigating the effects of preheating and stress relieving on the physical properties of welds in carbon-molybdenum pipe, an effort was made to measure the residual stresses in a stress-relieved and in a nonstress-relieved weld. Welds were made without external restraint in 6 in. diameter by $^9/_{16}$-in. wall A206 pipe using $^1/_8$-in. E7010 electrodes for the first pass and $^5/_{32}$-in. E7010 electrodes for subsequent passes.

Weld "A" was neither preheated nor stress-relieved. Weld "B" was preheated to a minimum temperature of 400° F. before welding (the preheat was maintained

Table 10—Properties of Welds in $^3/_4$-In. Thick A182 Carbon-Molybdenum Flange Material

		Tensile test results			*Diamond pyramid hardness, 10-kg. load*			
					Weld		*Heat-affected zone*	
Welding conditions	*Side-bend test results*	*Ultimate strength, psi.*	*Elongation in weld, %*	*Total elongation, %*	*Max.*	*Av.*	*Max.*	*Av.*
E7010 Electrodes, no preheat, no stress relief	Failed; crack propagated from root	78,600	35.0	17.3	209	187	262	249
E7010, no preheat, 1200° F., 1 hr. stress relief	Passed; no defects	75,700	38.5	21.7	224	208	235	209
E7010, 400° F. preheat, no stress relief	Failed; crack propagated from root	74,400	32.8	17.6	196	188	235	219
E7010, 400° F. preheat, 1200° F., 1 hr. stress relief	Passed; no defects	78,000	43.1	24.3	202	181	232	205
E7010, no preheat, 800° F., 24 hr. postheat	Passed; small root crack	81,700	40.6	24.3	224	212	266	247
E7015, low-hydrogen electrodes, no preheat, no stress relief	Passed; no defects	80,200	45.2	24.6	199	189	306	276

NOTE: All tension failures occurred in the weld metal. Hydrogen "fisheyes" were observed in tension fracture of E7010, no preheat, no stress relief weld. Hardness impressions were made every 0.5 mm. through the heat-affected zone and into the last pass weld metal. Diamond Pyramid Hardness of parent metal was 180.

between passes), stress-relieved at 1200° F. for 1 hr., and cooled in the furnace to 600° F. Residual stresses in the welds were determined from measurements with SR-4 strain gages using the strain relaxation technique. In this technique, gages are cemented to the surface of the material, and the change in strain is recorded after small blocks of metal containing the gages have been removed.

In these tests, four sets of type A-7 gages were mounted at quarter points around the circumference of each weld. In each set, one gage was placed parallel to the weld (circumferential) and one was placed perpendicular to the weld (parallel to the pipe axis). This arrangement permitted a determination of the residual stresses in both the axial and circumferential directions.

The stresses calculated from the strain readings are given in Table 11.

Table 11—Residual Stresses in Welds in 6 In. Diameter by $^{3}/_{16}$-In. Wall A206 Carbon-Molybdenum Pipe

($^{1}/_{8}$-in. E7010 electrodes used for the first pass and $^{5}/_{32}$-in. E7010 electrodes used for subsequent passes)

Weld No.	*Welding conditions*	*Range of circumferential stress, psi.*	*Range of axial stress, psi.*
A	No preheat, no stress relief	−2600 to −12,600	−26,400 to −40,900
B	400° F. preheat, 1200° F., 1 hr. stress relief	−1530 to +1990	−590 to −4270

The presence of high compressive stresses at the surface of the nonstress-relieved weld was somewhat contrary to expectations. Measurements of residual stresses at the surface of butt welds in flat plate generally indicate tensile stresses of yield point magnitude parallel to the weld and tensile stresses of various magnitudes perpendicular to the weld, depending upon the restraint imposed on the joint. However, if our interpretation of the data in Table 11 is correct, it appears that in circumferential pipe welds the shrinkage of the weld metal in the circumferential direction induces considerable bending of the pipe wall in the longitudinal direction. This would produce the effect indicated in the sketch in Fig. 2 greatly exaggerated. Again, if the interpretation of the data is correct, it would be expected that the application of internal pressure to the pipe, as in service or in hydrostatic testing, would cause plastic flow in the vicinity of the weld in the longitudinal direction, at least on the inside where there is probably a balancing high-tensile stress approaching yield point magnitude. Thus, it does not appear unreasonable to assume that after one such cycle of loading there would no longer be a high residual tensile stress on the inside wall of the pipe.

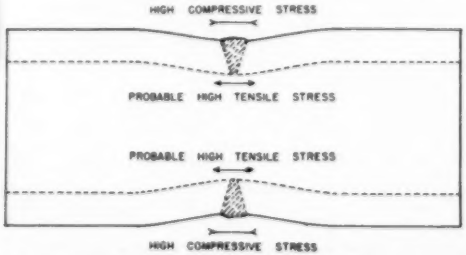

Fig. 2 Schematic diagram showing origin of lateral residual stresses caused by circumferential shrinkage of a butt weld in pipe

METALLOGRAPHIC EXAMINATION OF WELDS

Metallographic examination of specimens from welds 3, 6, 10 and 12 (Table 4) revealed only that welds made with preheat had a somewhat wider heat-affected zone and a considerably coarser grain structure in the weld metal than welds made without preheat. Stress relieving for 1 hr. at 1200° F. had no apparent effect on the microstructure.

The microstructure of the as-received A.S.T.M. A206 parent metal consisted of coarse lamellar pearlite in a ferrite matrix, typical of a low-carbon steel which has been cooled slowly from above the critical temperature.

Inasmuch as the metallographic examination of these welds failed to show any significant beneficial effects of either preheating or postheating it was considered valueless to present photomicrographs in this discussion.

CONCLUSIONS

The following conclusions are based upon the results of this investigation:

1. Transverse tensile tests and side-bend tests, at room temperature, of welds in A.S.T.M. A206, A161 and A182 carbon-molybdenum steels, made with A.W.S. Class E7010 electrodes, without preheating and/or stress relieving, showed evidence of weld metal embrittlement. This embrittlement, which is revealed at room temperature, under slow rates of strain, is believed to be caused by the presence of hydrogen absorbed from the arc atmosphere during welding.

2. Hydrogen embrittlement of E7010 weld deposits was not revealed by free-bend tests at room temperature, nor by Charpy keyhole notch impact tests at temperatures ranging from +76 to −90° F. However, indications of such embrittlement were obtained in Charpy tests at temperatures from +75 to +220° F.

3. Preheating at temperatures as high as 700° F. was only partially effective in preventing embrittlement of E7010 weld metal.

4. Postheating either at 1200° F. for 1 hr. (stress-relieving), or at lower temperatures for extended periods (800° F. for 24 hr.) was effective in eliminating weld metal embrittlement. Stress-relieving lowered the tensile strength whereas the lower-temperature, longer-time postheating did not.

5. Hardness measurements were found not to be acceptable criteria of the ductility of welded joints.

6. The free-bend test, required by the A.S.M.E. Boiler Code for qualification of welding procedure, was found to be of questionable value as an indicator of the ductility of welded joints. This test rejected high-strength weld metal and passed low-strength weld metal without regard to the intrinsic ductility of each. Other tests indicated the possibility of a situation where a weld metal of higher strength than the parent metal would definitely be preferable to a weld metal of lower strength, even though the latter were to have a higher ductility. Because of limitations in choice of available electrodes it might be difficult to produce the most desirable type of joint and still be able to pass the free-bend test.

7. Preheating and stress relieving are not necessary for welds made with certain low-hydrogen electrodes in A206, A161, A234 and A182 carbon-molybdenum steels in thicknesses up to at least $^3/_4$ in., regardless of the service temperature.

8. Of the low-hydrogen welds tested, those having a manganese content above 1% appeared to be superior in quality to those in which the manganese content was less than 1%.

9. For service temperatures above 800° F., preheating and stress relieving are not necessary for welds made with cellulose-coated, Class E7010, electrodes in A206, A161, A234 and A182 carbon-molybdenum steels in thicknesses up to at least $^3/_4$ in.

10. In cases where qualification welds made with E7010 electrodes in carbon-molybdenum steels do not, in the as-welded condition, pass the side-bend test required by the codes it should be permissible to postheat the test welds for 24 hr. at the intended normal operating temperature of the equipment. If welds treated in this manner will pass the side-bend test it should be permissible to place the welded joints into service in the as-welded condition. Otherwise, preheating and stress-relieving should be required.

ACKNOWLEDGMENTS

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The permission of Standard Oil Co. (Indiana) to publish this work is greatly appreciated as is also the backing and encouragement given throughout the investigation by G. W. Watts, Director of Engineering.

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PRACTICAL WELDER AND DESIGNER

Grain Blower

THE main housing for the grain blower is fabricated from 1/4-in. plate. The front or shaft end plate only is fillet welded to the rim at first. A hole is cut in the center of the front plate for the drive shaft. A center hole, half the diameter of the disk, is cut in the back plate to act as an intake port. Four-inch pipe is cut out to clear the diameter of the impeller and welded to the housing as shown.

The pipe flange indicated should be of sufficient diameter to accommodate the bearing housing pipe. The bearing housing is plain pipe or tubing bored for a snug fit to the outside diameter of the double-row ball bearings.

The impeller is built from 1/4-in. plate. The outer side has an intake port hole cut in the center as shown. Four vanes are fillet welded to these two disks with accurate 90° spacing. The drive shaft is fillet welded to the impeller assembly as indicated. After welded assembly, the entire impeller is machined all over in a lathe for balance. At this time the shaft is turned to fit snug in the bore of the double-row ball bearings.

In final assembly, the impeller is inserted in the main housing and the back plate of the housing fillet welded to the rim. After mounting in the split-clamp bracket, the V-belt pulley is mounted on the drive shaft.

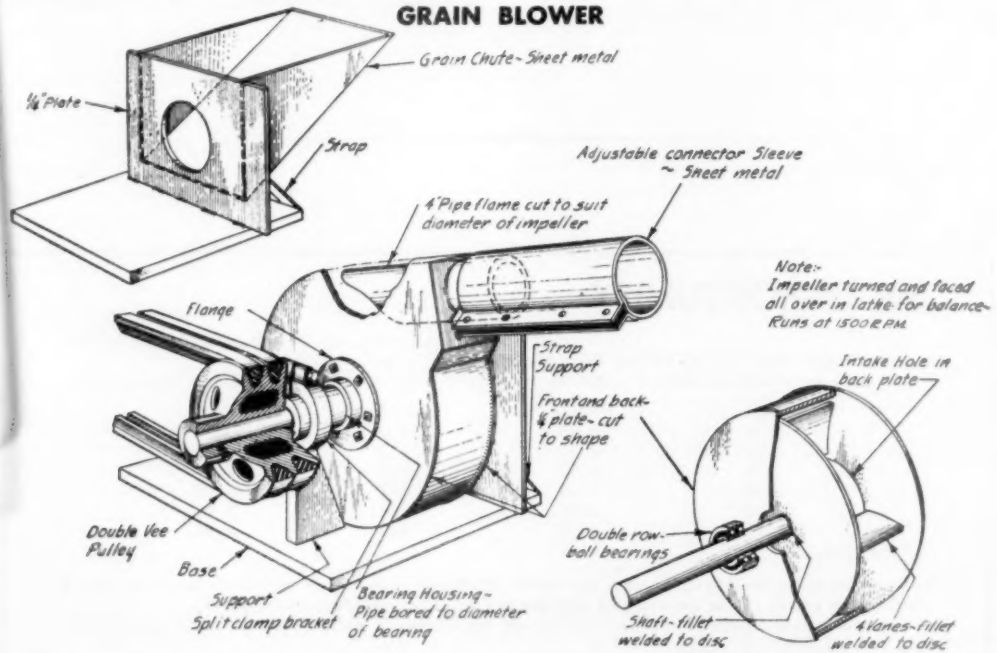
The bearing housing is a slip fit in the split-clamp bracket, permitting the grain blower to be set at any desired angle.

Lastly the grain chute made from sheet iron and reinforced with straps is welded in place. Also an adjustable connector sleeve of sheet metal can be made as an adapter for any desired application. Grease fittings or oil cups should be installed to provide lubrication to the bearings.

Bill of material:

1/2-in. plate	V-belt sheave
1/4-in. plate	Split clamp
20-gage sheet iron	Pipe flange
4-in. pipe or seamless tubing	Miscellaneous machine screws and nuts
1 1/2-in. shafting	1/8- x 1-in. steel straps
Double-row ball bearing	

A grain blower having a 16-in. diameter impeller and operating at 1500 rpm. will throw grain to a height of 40 ft.


GRAIN BLOWER
Grain Chute - Sheet metal
1/4" Plate
Strap
Adjustable connector Sleeve - Sheet metal
4" Pipe flame cut to suit diameter of impeller
Note:- Impeller turned and faced all over in lathe for balance - Runs at 1500 R.P.M.
Flange
Strap Support
Front and back - 1/4" plate - cut to shape
Intake Hole in back plate
Double Vee Pulley
Base
Support Split clamp bracket
Bearing Housing - Pipe bored to diameter of bearing
Double row - ball bearings
Shaft - fillet welded to disc
4 vanes - fillet welded to disc

Sketch and Data Courtesy Lincoln Electric Co.

Plug Welding Technique in Clad Lining Dough Mixer Bowls

by W. M. Davis

IMPROVED operations and a 30% direct labor saving are the results of a plug welding technique developed for welding stainless steel clad sheet to heavy gray iron castings used as end members on a large mixing bowl. By welding through 50 holes bored in the casting, through to the steel backing of the clad sheet, the interior of the bowl is smooth, and clad sheet and casting are firmly bonded so as to eliminate loosening in operation.

The welding technique has had more than a year's trial in the manufacturing plant of the J. H. Day Company, Incorporated, Cincinnati, Ohio, makers of mixing equipment for the bakery, chemical, paint and allied fields, where the method was originated.

The new method has saved 30% in assembly time over previous methods, has eliminated all complaints from the field concerning loosening liner sheet, loosening machine screws, loosening and breakage of screw heads inside the mixing drum. Since the welded stainless steel lined bowl is easier to clean to strict sanitary codes, it is thus more economical to operate.

The bowl is the mixer drum on a 50-hp. dough mixing machine used in the baking industry. The drum holds 1600 lb. of dough which is mixed by agitator blades on a Monel shaft, turning on bearings in the end castings. Welding is important in the construction of this bowl not only because of the need for strength and rigidity under severe operating stresses, but also because all possibility of foreign metal dropping inside must be eliminated. A dropped rivet or a bolt head could easily find its way into a loaf of bread.

In considering the design of the drum a lap-welded lining, welded on the inside, was discarded because of the cost of grinding to produce a smooth finish and the danger of grinding through the cladding into the backing. The present method of welding through the end casting was devised as an alternative.

The end castings are about 4 ft. in diameter, irregular in shape and thickness with one side machined for the stainless 302-type clad lining. The end sheets must be rigidly and permanently attached to the end castings because the end castings support the gross weight and carry the main agitator shaft, associated drive and bearings.

The end sheets are $^3/_{16}$ in. thick and formerly were bolted to the casting with fifty $^3/_8$-in. stainless steel flat head machine screws. The casting and sheet were clamped together along with drilling template and all the holes were drilled on a radial drill. Each end then had to be turned over and precision countersunk for the flat head machine bolts. Heads under or over could not be permitted.

As a measure against the loosening problem in the field, the sheet was slightly peened or prick-punched into the screw slots after severe tightening. Extreme tightening against loosening increased the possibility of heads snapping off inside the drum. A system of fastening the sheet to the casting with stainless steel rivets was tried and then abandoned because of the common difficulty in getting stainless steel rivets to "draw" as would soft iron rivets.

Adoption of the welding method seems to have solved all previous problems as well as to reduce cost and produce a better machine.

Fifty $^3/_8$-in. holes are bored as before except they are bored only through the casting. There are no holes in the liner sheet except around the shaft, and here the heads of machine screws are held in by the hub of the agitator. Three other holes in each head are used for bolting the Monel spacer shaft in the drum. Fifty holes in the liner sheet have been eliminated. Each drilled hole in the casting is thenc ounterbored to $^{11}/_{16}$ in. in diameter to within $^1/_4$ in. of the smooth side, regardless of the thickness of the casting at the point of the hole. Thus, although the casting varies in thickness from 1 to almost 2 in., the $^3/_8$-in. portion of the hole is always $^1/_4$ in. deep from the smooth side. (See Fig. 1.)

To make ready for welding in the holes after drilling, two end castings are clamped together with smooth sides face-to-face and the two clad sheets in position in the center (Fig. 2). The welder fills the holes on one side, then turns the clamped assembly over and welds the other side.

One rod of Lincoln Ferroweld is used to each hole although the weld does not use up the entire rod. Use of a new rod for each hole eliminates the possibility of starting with a short piece and having to change rods

W. M. Davis is Welding Engineer, with the Lincoln Electric Co. and is located in Cincinnati, Ohio.

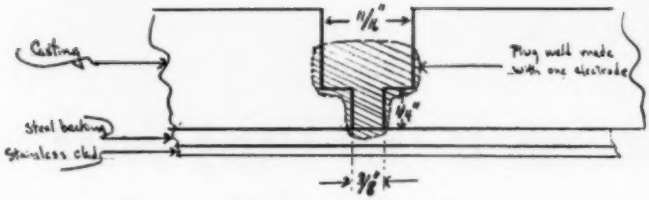


Fig. 1 Method of applying liner

and make two welds in a single hole. The freshly exposed cast iron in the bored holes presents a clean surface for a good electrode deposit from this mild steel electrode designed for cast-iron welds. Welding time is 45 min. for each side, less than 1 min. per hole, a saving of more than 30% in time as compared with the bolting method.

The fact that each hole is completed in less than a minute contributes to keeping down excess heat in the casting and reduces the chance of introducing stress between casting and liner (Fig. 3). The small weld area also reduces the danger of excess stress due to the different rates of shrinkage in the cast iron, electrode metal and the steel side of the clad.

Fusion is made with the steel side of the clad sheet

Fig. 2 Stainless clad sheet is plug welded to cast end plates of mixer

For welding, two plates are clamped together with clad sheets between, welded on one side and turned over. Each end plate is welded in 45 min.

Fig. 3 This shows the outside appearance of a completed end welding job and also the two welded seams for the water cooling jacket at the left

Insulation is added to the outside to bring it up to top edge of casting and then a stainless sheet is welded over the outside. Hub in casting carries antifriction bearings and drive mechanism which is later enclosed in the finished machine, making it unnecessary to fill weld holes to present a flush appearance

Fig. 4 Spots on the end sheet are the only visible inside evidence of the outside welding through the holes

These spots are easily removed with a buffer. Holes around the shaft opening and three holes above for horizontal bar brace are now the only holes in the liner sheet. Edge welds inside are ground to give rounded corners inside the drum for easy cleaning and sanitation

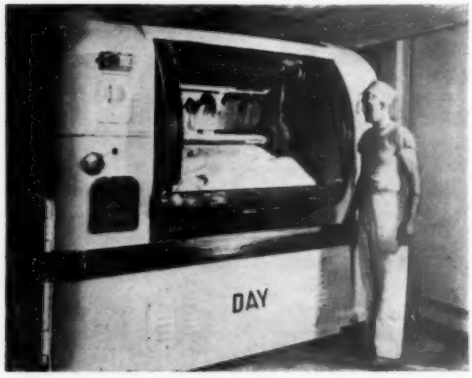


Fig. 5 Completed dough mixer in action in a bakery plant

The end surface inside the bowl and in front of the right agitator hub shows the smooth finished surface obtained by a plug welding from the other side. The drum holds 1600 lb. of dough

with a low quantity of heat while a strong seat for the weld is provided by filling the hole to a point above the square shouldered bottom ledge of the counterbore. The system is, in effect, a welded shoulder plug welded from the outside of the vessel instead of inside and without penetrating the inside lining (Fig. 4). The finishing time on the inside is thus reduced on the end sheets to removing a slight discoloration with an ordinary buffer wheel.

More than 1000 new models of the Day dough mixer have been placed in service using the new method of bonding end sheets and company officials said that not one complaint has been received from owner or service mechanics (Fig. 5).

Welding versus Waste

by Dr. Alois Cibulka

WASTE of money—what a meaningless phrase. Money is nothing more than a convenient scale carried in the pocket for the sake of comparing the commodities of life. It cannot be wasted unless burned.

Waste of materials, and of labor shaping them for our daily use, is what really counts. And that is the engineer's responsibility. If he is not up to date, the human society should charge him with criminal ignorance and lock him up behind bars. Metals constitute the base on which our civilization is built and steel is the most precious of all. It is dwindling fast and also is being wasted in a most outrageous way, especially in the construction field.

Structural engineers are the most backward, ossified lot of the whole profession. Countless thousands of structures going up right now all over the world in this year of A.D. 1950 prove it beyond any doubt. These ignorant wastrals may do us more harm than all the so-called radicals of the world.

Suppose that the Russian engineers, forced by necessity—"Mother of the Inventions," adopt new methods of steel utilization and make every pound of steel do twice as much work or even more than our so-called engineers are doing because they prefer to scoot in the rut of conventional thinking, if it could be named thinking at all. Each pound of steel saved is actually twice better than the pound of the extra one made; you do not have to spend any material and labor for its production. What will happen then to our boasted superiority in production? And the Russian engineers do not have to experiment, spend time and labor in order to invent

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Fig. 1 Refer to December 1950 article for construction of these girders

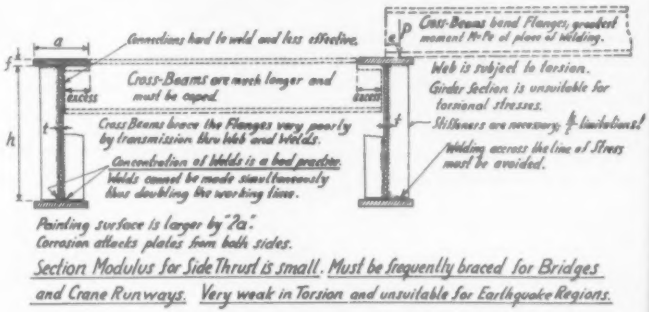
a
Connections hard to weld and less effective.
P
Cross-Beams bend Flanges, greatest moment M=Pa at place of welding.
Cross-Beams are much longer and must be coped.
excess
excess
Web is subject to torsion.
Girder Section is unsuitable for torsional stresses.
Cross Beams brace the Flanges very poorly by transmission thru Web and Welds.
Stiffeners are necessary, & insulations!
h
Concentration of Welds is a bad practice.
Welds cannot be made simultaneously thus doubling the working time.
Welding across the line of Stress must be avoided.
Painting surface is larger by "2a".
Corrosion attacks plates from both sides.
Section Modulus for Side Thrust is small. Must be frequently braced for Bridges and Crane Runways. Very weak in Torsion and unsuitable for Earthquake Regions.

Fig. 2 Illogically constructed welded girders

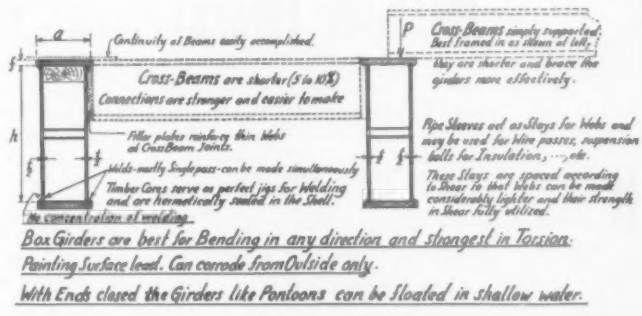
a
Continuity of Beams easily accomplished.
P
Cross-Beams simply supported, but framed in as stiffners at ends; they are shorter and brace the girders more effectively.
Cross-Beams are shorter (5 to 10%)
Connections are stronger and easier to make
h
Pipe Sleeves act as Stays for Webs and may be used for wire passes, suspension bolts for Insulation, ..., etc.
Fillet plates reinforce thin Webs at Cross Beam Joints.
Welds-easily Single-pass-can be made simultaneously
Timber Cores serve as perfect jigs for Welding and are hermetically sealed in the Shell.
These Stays are spaced according to shear so that Webs can be made considerably lighter and their Strength in shear fully utilized.
No concentration of welding.
Box Girders are best for Bending in any direction and strongest in Torsion.
Painting Surface lead. Can corrode from Outside only.
With Ends closed the Girders like Pontoons can be floated in shallow water.

Fig. 3 Logical design of welded girders

such new methods; they were already invented and proved beyond any doubt, beyond any question for them, thanks to our own engineers. Welding Process logically applied will do wonders for them; it opens now, never-dreamed of possibilities to all who can see the light. Welding can produce bridges, hangars, water dams, skyscrapers, sea walls, towers, drilling rigs, piling, wharves, rigid frames, industrial buildings, road pavings, houses and many other structures with one-half to one-tenth of the steel used by the obsolete conventional designs of today, and much better structures at that. Welding made the dream of the structural engineer come true; box sections are supreme in his design and can be made now even cheaper than the other kinds. High cost of materials and labor is a very poor alibi for the ignorance and mental laziness of our engineers.

If I can save a large amount of steel in a building it is perfectly ethical and legitimate in any competitive society to offer a good premium of one or two cents per pound of steel, pay an extra 25 cents an hour to the welders in order to finish the job on time. The customer is well satisfied and willing to pay more than a 5% fee as he is saving far more all around.

On a comparatively small industrial building shown in Fig. 1, 32 tons of steel were saved on the four frames alone. The spans are 65 ft. and will carry another story later; the bracing is for erection only. The auto dealer would have to sell a good many cars to make up the six thousand dollars he saved by listening to someone else's sales talk. And he could have saved much more and gotten a better looking job if he used architectural aluminum with welded seams instead of brick for the 16-ft.-high walls.

Figures 3 and 4 tell the story of what welding can do if logically applied.

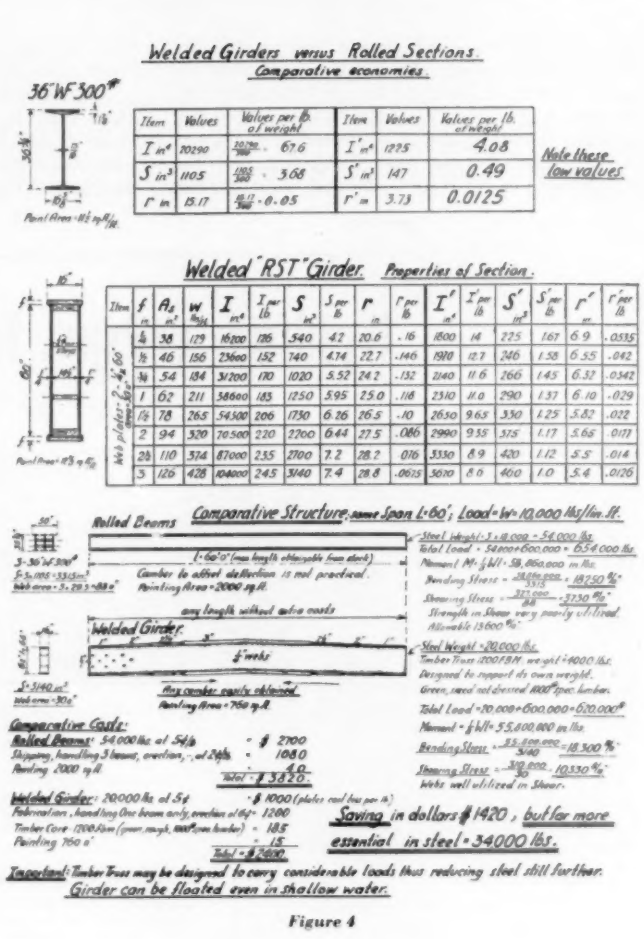

Figure 4

Methods Improvement in Welding

by S. Tilles

ONE of our welding instructors, Louis Birinyi, has recently completed an extensive library survey of current literature on methods improvement in the welding field. One of the results of this investigation, which was done under my direction, is the enclosed set of check-list questions. These have been specifically designed to direct the welding engineer's thoughts along those lines that have been shown to be most productive of job improvements.

You will note that these are grouped into 5 different classifications. This is in accordance with our ideas on methods improvement here at Purdue. We consider all improvements as involving a change in one of the following areas (see *Systematic Motion and Time Study* by M. E. Mundel, Prentice-Hall, 1947):

1. The hand and body motions.

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2. The workplace layout.
3. The over-all process.
4. The product design.
5. The materials used.

CHECK-LIST QUESTIONS

Classification One

1. Are the following parts of the operator's body adequately protected: (*a*) eyes, (*b*) arms, (*c*) torso, (*d*) legs?
2. Would in-service or other types of training increase the capacity of the operator?
3. Is the job being done in the prescribed manner?
4. Does the operator accept only properly fit work for processing at his station?

Classification Two

1. Are the following factors of environment conducive to the performance of work: (*a*) ventilation, (*b*) lighting, (*c*) temperature?
2. Does the workplace layout permit maximum use of the welder?
3. Do the tools of the workplace suit the work requirements?
4. Does the workplace provide for a continuous flow of incoming and outgoing material?
5. Does the workplace have adequate material-handling devices?
6. Can fixtures supplement or reduce handling?
7. Would a simple positioning fixture help the operator perform his task?
8. Can jigs be used for holding during welding?
9. Are accurate jigs being used?
10. Do the jigs provide easy access to the weld joint?
11. Are the jigs easy to load and unload?
12. Can an automatic work positioner and mover be used?
13. Does the welding equipment have an adequate capacity?
14. Are the various parts of the welding equipment functioning properly?
15. Is there adequate welding control for the job being done?

Classification Three

1. Has the welding procedure been planned?
2. Is the planning adequate in scope?
3. Does planning of the fabricating procedure originate in the engineering department?
4. Has production been planned to utilize the workers' familiarity with specific jobs?
5. Does the layout efficiently execute the pre-planned operations?
6. Has the personnel been sold on the method of processing being used?
7. Does the volume of work lend itself to straight-line production?
8. Can the welding and fitting on any one sub-assembly be established as straight-line production?
9. Can the work be simplified by using more sub-assemblies?
10. Does the volume of work warrant the introduction of new machines and fixtures?
11. Does the volume of work justify expensive jigging?
12. Does the flow of work between stations proceed smoothly?
13. Can operations be rearranged or eliminated through use of more accurate jigging?
14. Would the use of more accuracy in preparation of parts permit the elimination of tack welding?
15. Can an improved cleaning procedure lead to less operations?
16. Does the required accuracy of work demand more inspection during processing?
17. Is the welding sequence an efficient one?
18. Is waste occurring because of poorly fit work?
19. Can the weld be done automatically? Will this add space for other operations?

Classification Four

1. Is the part designed for functional efficiency?
2. Would a redesign of the part provide better welding conditions?
3. Would a redesign of the part permit simpler jigging to be used?
4. Would a redesign ease assembly problems?
5. Does the design provide inherent part support?
6. Is the most efficient weld joint being used?
7. Are the weld joints readily accessible?
8. Does the weld joint design conform to welding process limitations?
9. Will this new design be producible with existing plant equipment?

Classification Five

1. Will a redesign eliminate any of the welding cost?
2. Could welding be eliminated by using a new material?
3. Would the use of a different material provide better welding conditions?
4. Would a combination design using several welding processes for joining improve the product or reduce its cost?
5. Should the part be redesigned for joining by another welding process?
6. Can a combination of materials be used?
7. Is the purchased material of the right size? Would a different size of purchased material reduce fabricating costs?

Hole Digger

THE lower half is cut from an automobile rear end as shown and the axle allowed to extend a length of 30 in. or more.

The post hole auger can be an old conveyor or stoker screw welded to 4-in. diameter pipe. The length of auger can be modified to suit the maximum depth of hole to be dug or the material available. The end of the center pipe is slotted and hammered to a point for welding to the shaft. The spacer shown is for supporting the pipe from the axle. A grease seal is suggested to prevent lubricant from working out.

The straps and braces are cut and bent to shape and then fillet welded to the rear end axle housing as shown.

The adjustable upper guide is of telescoping construction, affording correct length to maintain the post hole digger in a vertical position. The locking bolt shown is inserted after the post hole digger mechanism is mounted on the tractor.

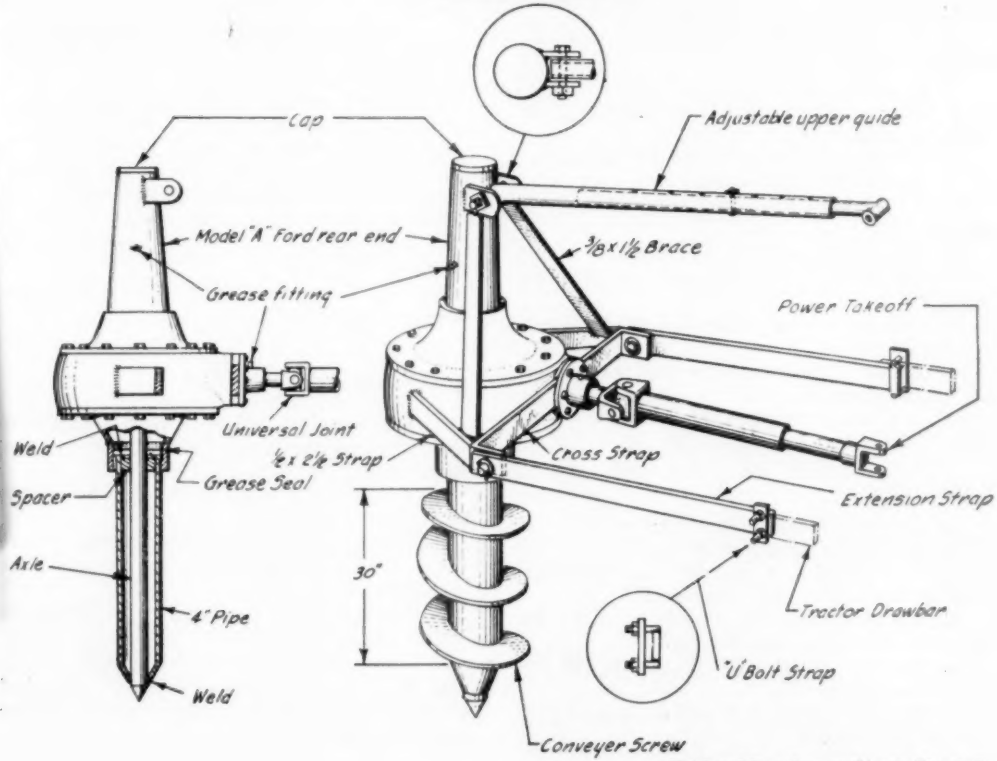
A power take-off equipped with a universal joint as available on any farm tractor can be used to drive the auger. Grease fittings should be provided on all bearings to minimize friction and thereby reduce wear on the moving parts.

"U" bolts are used to permit easy mounting and dismantling from any conventional tractor extension.

Bill of Material:

Automobile rear axle	$1^1/_4$-in. pipe
$^3/_8$- x $1^1/_2$-in. steel strap	$^3/_4$-in. pipe
$^1/_2$- x $2^1/_2$-in. steel strap	Conveyor or stoker screw
2 "U" bolts	2 grease fittings
4-in. pipe or seamless tubing	Miscellaneous bolts, screws, washers and cotter pins
$3^1/_2$- x 3-in. bar stock	

HOLE DIGGER



Sketch and Data Courtesy Lincoln Electric Co.

Surface Preparation for Metallizing Shafts and Similar Objects

by Peter G. Dennison

THE Dennison method of surface preparation for metallizing shafts and similar objects, heretofore unpublicized, was developed in 1938 and kept as a trade secret by our company. Recently in the interest of better metallizing, it was presented to the members of the American Metallizing Contractors Association. It is herein described in detail for the benefit of all metallizers, who are free to use it without obligation.

In the early days of metallizing, sandblasting was the customary manner of preparing surfaces which were to receive metal-sprayed deposits. This often provided an unsatisfactory bond, as more often than not the sprayed metal peeled off readily.

Later it developed that a rough torn thread was more efficient than sandblasting, but such threading was more or less limited to the soft easily machined materials. When shafts of harder and tougher steels were prepared, it was difficult to cut a satisfactory thread, so blasting over the threads was often resorted to, but there were still too many failures with metallized coatings.

Peter G. Dennison is President of the Metal Spraying Corp., Milwaukee, Wis.; also a member of the AMERICAN WELDING SOCIETY Committee on Metallizing.

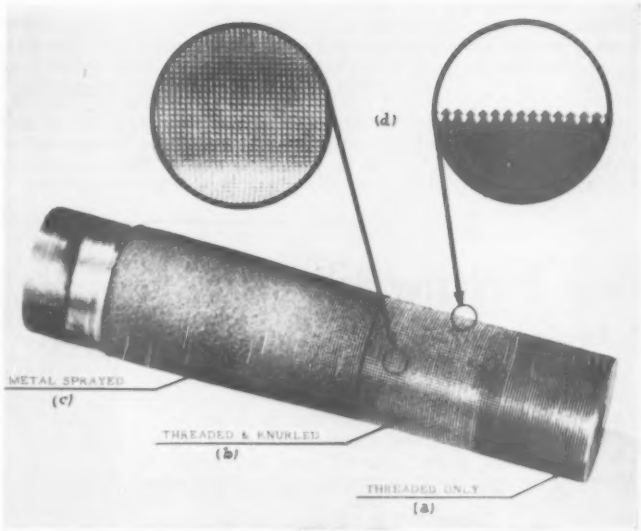


Figure 1

As industry became more dependent upon metallizing for salvaging worn parts it became imperative that more dependable bonds be developed. After much experimenting with threads of various sizes, shapes and angles and with knurls of different designs a combination of both was devised that has stood the test of many years in service.

Since the particles of molten sprayed metal are practically of one size, a standard of 20 threads per inch was established

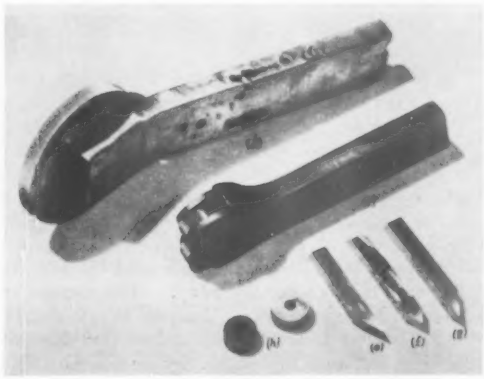


Figure 2

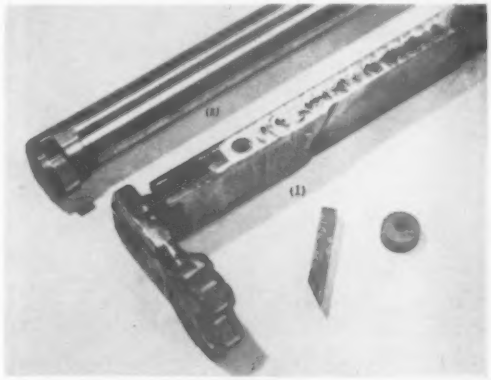


Figure 3

as illustrated in Fig. 1 (*a*). The ideal thread for comparatively soft metals was found to be one that was cut with a 30-degree included angle threading tool Fig. 2 (*e* and *g*).

These standards were then modified to suit the various degrees of hardness found in different base metals. The number of threads per inch and the angle of the threading tool ranged from 20 threads per inch with the 30-degree tool to 16 threads per inch with the 60-degree tool used for the harder base metals.

The bond of the sprayed metal coating after such surface preparation proved so satisfactory on steel that it was tried on cast iron. Here it was found that the 30-degree threading tool and 20 threads per inch were best for hard cast iron while the 60-degree tool and 16 threads per inch were best for soft, crumbly cast iron.

After the threads are properly cut (see Fig. 1 (*a*)), the tops of the threads are knurled to a depth of about 0.015 in. to obtain the type of surface shown in Fig. 1 (*b*). An enlargement of the surface after threading and knurling is shown in Fig. 1 (*d*). The "hooks" are spaced at twenty per inch circumferentially when made with a twenty-pitch knurl and can be felt with a knife point or a finger nail.

The knurls (Fig. 2 (*h*)) are standard 20- or 21-pitch straight knurls. For external work one or two knurls mounted in a knurling tool holder (Fig. 2 (*i*)) may be used for both continuous and interrupted surfaces. For shoulder fits a larger knurl should be mounted in a bent or offset holder. The knurling tool shown in Fig. 2 (*j*) may be used for either right- or left-hand shoulders.

Inside diameters require a different procedure. The threading tool is mounted at an angle so that the back face of the thread is at right angles to the bore, thus cutting a buttress or ratchet thread. For large bores the 30-degree tool bit is used, while for smaller bores the 60-degree tool bit should be used to prevent the sprayed metal from bridging over from thread to thread. In either case, the knurling tools shown in Fig. 2 (*k* and *l*) are used in the same manner as for external work.

This method of preparation, using rough threading and knurling, has many advantages. It is simple, very efficient and economical. The tool bits are easily ground from standard stock and the knurls may be obtained through almost any machine-tool supplier. After threading and knurling the surface is immediately ready for spraying. No sandblasting or other treatment is required and it is only necessary to mount the metallizing gun in the toolpost and proceed in the usual manner. Normally no dovetails are necessary at the ends of the undercut since each thread and knurl notch carries its share of the the load in preventing the lifting of the sprayed metal. The same is true of any corners of keyways.

The sprayed metal is so firmly bonded to the base metal that keyways may be cut in the normal manner using end mills, or circular milling cutters.

Flame Hardening Bulldozer Blades

BULLDOZER blades that will wear six times as long as ordinary blades are being produced by a Seattle, Wash., firm by flame hardening. The flame hardening is done automatically at high speeds.

Two flame-hardening heads, mounted on a CM-37 machine, are used to harden both edges of the center blades simultaneously. The job is done while the blades are still hot from a bevel-cutting operation, so warping is eliminated. The depth of hardness is $^1/_4$ in., and the speed of hardening is 10 to 12 in. per minute. The amount of hardness obtained is 550 to 600 Brinell.

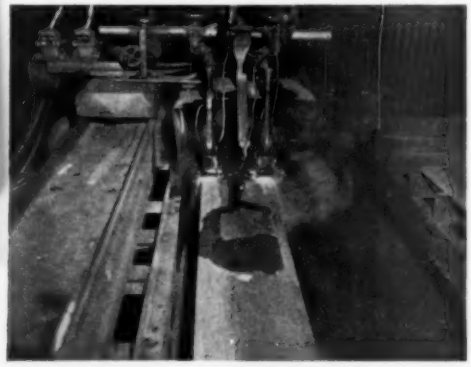


Fig. 1 Two edges of a bulldozer center blade are being flame hardened simultaneously by two Oxweld flame-hardening heads mounted on a CM-37 machine

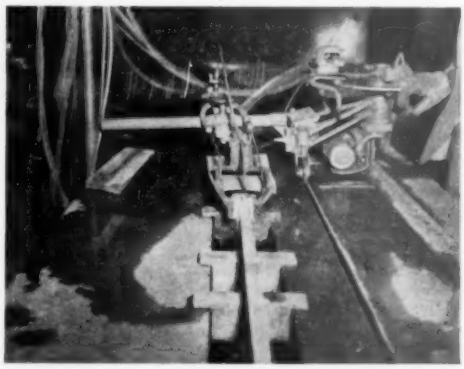


Fig. 2 This bulldozer side-cutter blade is having both sides of one edge hardened by two flame-hardening heads mounted on a CM-16 machine. The hardening speed is 9 in. per minute

Side-cutter blades are handled a little differently. Both sides of one edge of the blades are hardened at the same time. Two flame-hardening heads, mounted on a CM-16 machine, are used. The depth of hardness is $^3/_{16}$ in. and the hardening speed is 9 in. per minute. The amount of hardness is the same as the center blades.

The blades are made from rolled steel. Blade sections up to 21 ft. long, 10 in. wide and 1 in. thick, are hardened in this automatic setup. These sections are then cut to desired lengths. Isaacson Iron Works, the manufacturer, reports that demand for these blades is so great that often two shifts a day are necessary to fill orders.

Welded Tanks

PRODUCTION of the new, redesigned and standardized M-47 General Patton army tanks is expected to be rapidly accelerated as Baldwin-Lima-Hamilton Corp., now ahead of schedule, brings tank hull production up to full scale. By July 1st the Eddystone plant will have reached this rate.

The first ten new hulls for these 45-ton medium tanks were shipped to the Detroit Arsenal in late March while there were still close to 350 of the several thousand World War II models of these tanks remaining to be rebuilt there.

Baldwin got into the tank production program early. Its first order for tank hulls on August 15th came within a month after plant facilities were offered for government use. Combined with other orders since that date, the company now has government contracts totaling $60,000,000 for continuing the tank production program.

Tooling up for first production was done without waiting for machines and materials for jigs, fixtures and welding positioners. Since steel was difficult to obtain, the whole Baldwin plant was surveyed for materials that could be salvaged and fabricated into production equipment. Although new welding machines, welding positioners and other new machines were necessary, a large saving of time and expense resulted from the accumulation of about 300 tons of salvaged steel plates, bars, bolts, angles, etc., which were turned into jigs and fixtures. Typical of this project are the shop-fabricated welding positioners.

All assembly work was reduced to fixture regulation in order to assure repetitive accuracy of parts assembly for welding without highly skilled workmen. Completely new sets of jigs and fixtures were required because of changes and improvements in tank design since production ceased in 1945. Tooling was completed by January 1st, within four months after the first order.

Not the least important problem, however, was the job of developing personnel for production. It was decided early to set up the shop as a separate organization with all new people. As key personnel, a super-

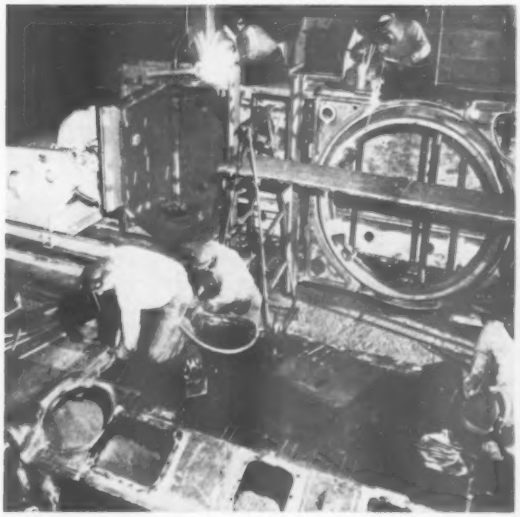


Fig. 1 (above) Assembly of M-47 General Patton army tank hulls is entirely welding, done largely on motor-driven positioners. Each hull requires 800 lb. of weld metal deposited from welding rods more than a mile in length

Fig. 2 (at right) Welding the tank turret in preparation for fitting it into the hull

intendent and three foremen were brought in from other departments. Also a few former supervisors who had retired consented to return to help with the program. Their responsibility was to develop an organization and train new men in readiness for the start of production. By April 1st more than 500 men were working on tank hulls.

Space for this plant was provided in the former steam locomotive boiler shop where operations had been discontinued because of conversion to Diesel locomotive production. Initial production was started in an area of about 110,000 sq. ft. This will be expanded by July 1st to 350,000 sq. ft. which will be divided into five 80-ft. wide bays, each about 875 ft. long. After this time there will be upwards of 150 continually in process of manufacture by welding and machining operations.

Handling on the production line is minimized by unidirectional flow of material, down one bay and back the next, and by the flexibility provided by motor-operated welding positioners for accessibility of joints for welding. Production line arrangement also minimizes interference between the two to four overhead cranes of 25 to 75 tons capacity each which operate in each bay. The size and weight of component parts of the tank hull require power lifting for roughly 95% of all handling.

A good measure of the magnitude of the job of fabricating the hulls is afforded by rough over-all dimensions and weights. Tank hulls, when completed with inside fittings, weigh about 15 tons and include 800 lb. of weld metal. The principal welds join 20 heavy steel plate members and eight steel castings to form a hull approximately 20 ft. long.

All welding is done by the electric arc method, using a.-c. transformer type welders of 500 amp. capacity. Welding electrodes are a special purpose shielded arc type for welding armor and range in size from $^{5}/_{32}$ to $^{5}/_{16}$ in. in diameter. The number of welding positioners for the assembled hull will be increased from 10 to 30 by July 1st. Figures 1 and 2 show a few of the welding operations.

After welding, the hull undergoes six separate machining operations. About 80% of the machining operations are done after assembly and welding. Principal operations before welding are planing the edges of the plates for weld joints and drilling holes in the side plates. Many of the machine tools were formerly used for machining steam locomotive parts, and were rebuilt for the special operations required in tank fabrication.

The first machining operation is boring and facing the transmission openings and the idler shaft sockets. Four horizontal boring and milling machines are provided for this job. Second is the milling of bottom slope sheets for attachment of suspension brackets for wheels. Horizontal boring and milling machines are used for this purpose.

After the tank hulls are machined they are loaded in pairs on flat cars for shipment to the Detroit Arsenal.

Largest Resistance Welder Installed at Ryan

GIANT strides in maintaining its position as one of the nation's leaders in resistance welding fabrication are being taken by Ryan Aeronautical Co. Already equipped with 39 machines—30 spot welders, 6 seam welders, 2 seam- or roll-spot welders and 1 flash welder—the plant is receiving additional special equipment which has been on order.

Just installed is the newest resistance welder in the plant and the largest machine of its kind in the country—a combination spot welder, roll-spot welder and seam welder. Three more seam welders are on their way.

Manufactured by the Federal Machine and Welding Co. of Warren, Ohio, to Ryan's specifications, the new machine can handle such heavy gages as two pieces of $^{1}/_{8}$-in. thick aluminum alloy, or two pieces of 0.156-in. austenitic or nickel alloy, to an unusually great "throat" depth—60 in.

Its installation resulted in an immediately accelerated output of wing fuel tanks for combat planes, the largest such tanks known to be in production. Although the welder will be used primarily for this project, it also will do work for theexp erimental and manifold departments, and on heavy jet engine components.

Soon after arrival of this huge machine, two others—only slightly smaller—were added to the rapidly expanding electrical welding facilities at Ryan. They are spot-welding machines, built by Thomson Electric Welder Co. of Lynn, Mass. Their throat depth is 48 in., and maximum pressure at the electrode is 7300 lb. They are guaranteed to weld two aluminum alloy pieces as thick as 0.102 in. each.

In contrast with the machines in the plant depending on single a.-c. current or electrostatic stored energy, the new welders are 3-phase to single-phase frequency con-

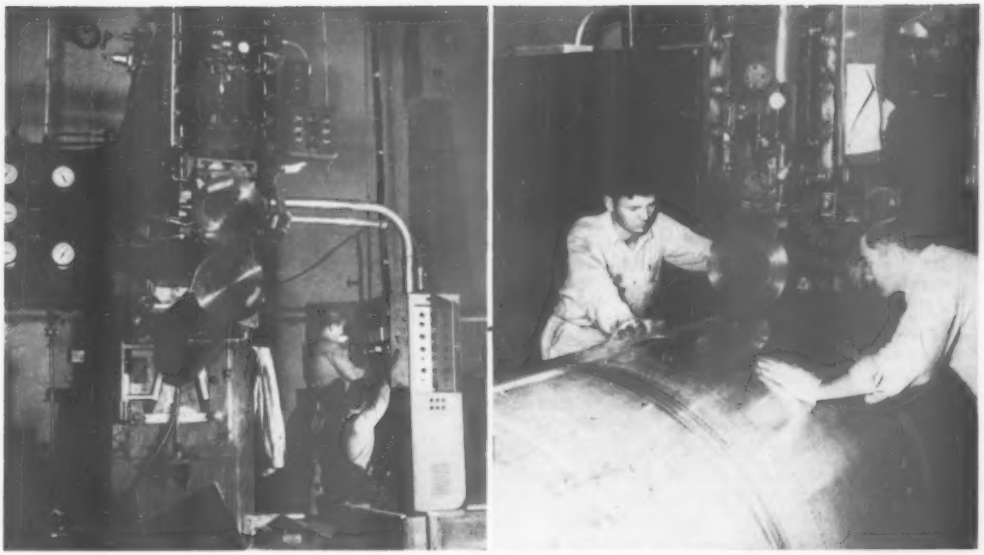


Fig. 1 Capable of producing continuous gastight seams in large metal structure, this newly installed welding machine will augment the seam-welding equipment which is operating at full capacity at Ryan Aeronautical Co.

verted type equipment, providing a more balanced load across the power supply and greater versatility for difficult welding jobs. They will be able to handle both aluminum and steel.

An idea of the power output of the new Federal Welder can be noted from the fact that its maximum is 105,000 amp. at the electrode on short circuit.

The maximum electrode pressure at 80 lb. line pressure is 9100 lb., almost twice the pressure of the next largest roll-spot welder in the plant.

One of the Ryan-specified features is aimed at guaranteeing safe operations. In the firing mechanism, the current is unable to pass until a predetermined pressure is developed on the electrode.

Another unusual control applies to seam welding. The first spot is made at reduced heat to avoid damage to the material, after which normal heat is automatically provided. This overcomes the tendency to overheat the first spot, occasionally resulting in excessive penetration of the weld nugget.

The weld size is firmly controlled by contoured wheels, and the wheel electrode is continuously "dressed" to avoid the time-consuming necessity of sending it to a lathe for turning, previously a requirement—usually after every 4 hr. use.

Two swinging arms, one for longitudinal and one for circular seam welding, are another valuable time-saver and safety feature. Without them, a crane would be needed to change the setup of the machine.

Special spot-weld adapters are provided for removing the wheel electrodes and installing standard spot welders with tips instead of wheels.

The machine has internally cooled wheels for seam welding of aluminum alloys and provision for "Flood" cooling of wheels when welding steel.

A special General Electric control panel has tap switches for changing the heat. One of the major features of this panel is its fixed increment heat control, making certain that specific power output can be duplicated exactly in the control settings.

Just before press time of the "Reporter," two Thomson seam welders, of 48-in. throat depth, and one federal spot welder, of 60 in. throat depth, arrived at the plant. All this new equipment will be used primarily for welding operations on the giant wing fuel tanks.

To furnish the greatly increased power requirements of the battery of welding machines, Ryan has augmented its electrical system by installation of a new underground cable, six large secondary distribution lines and a complete substation capable of supplying 1000 kva. at the required potential.

Tractor-Mounted Buzz Saw

THE tractor frame extensions are made from 3-in. channel bolted to the main tractor frame. A box-type sliding guide is built by butt welding a second 3-in. channel to the bolted extensions as shown.

The saw, saw drive-shaft, pulley and bearing housings can be secured at a farm implement store or in a salvage yard. The bearing housings may be supported on two 2- x 2-in. angles or bolted directly to the 3-in. channel extension of the main tractor frame.

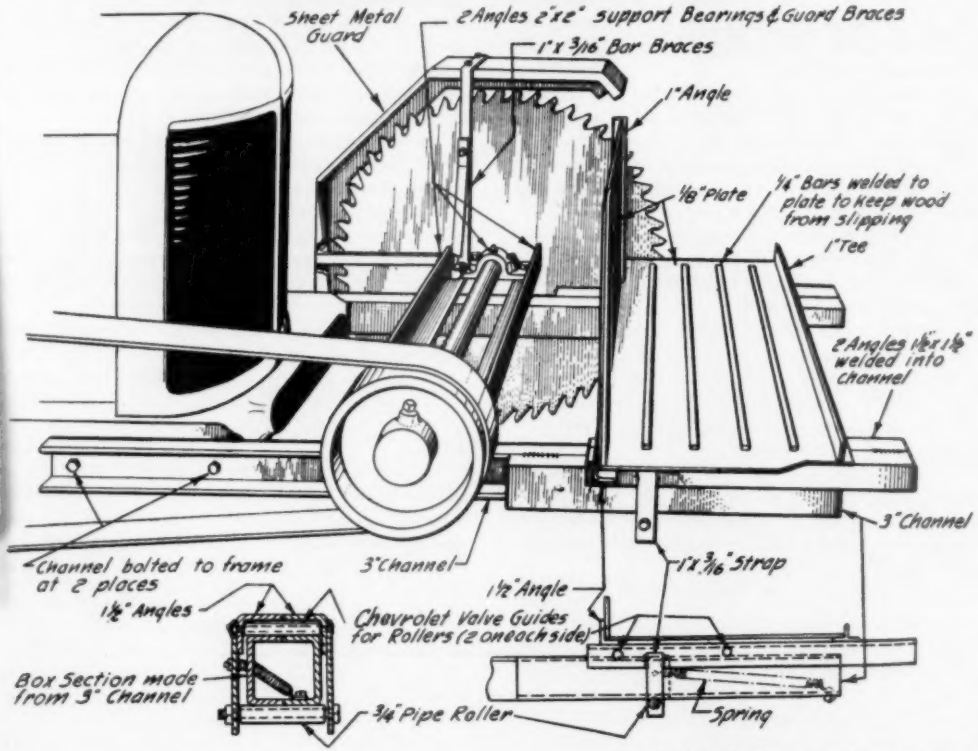
The sliding work support guide is built from $^1/_8$-in. plate and rides on four rollers made from automobile valve guides as shown in the bottom sketch. A spring is provided on each side to automatically return the sliding support to its starting position. The lower $^3/_4$-in. pipe rollers bolted to the $^3/_{16}$- x 1-in. straps are for holding the sliding work support to the main frame guide. The bottom bolts that anchor the springs also act as a stop to prevent the work support slide from coming off the guide.

Bill of Material:

$^1/_8$-in. plate
1-in. angle
$1^1/_2$-in. angle
2-in. angle
2-in. channel
3-in. channel
2 bearing housings
1 shaft
Flat belt pulley
$^1/_4$-in. round bars
$^3/_{16}$- x 1-in. steel strap
20-gage sheet iron
$^3/_4$-in. pipe
4 automobile valve guides
1-in. "T" strap
2 springs
Miscellaneous bolts and screws

A sturdy guard fabricated from sheet iron and steel straps helps to prevent accidents and should be included under all circumstances.

TRACTOR-MOUNTED BUZZ SAW


Sheet Metal Guard
2 Angles 2"x2" support Bearings & Guard Braces
1"x 3/16" Bar Braces
1" Angle
1/8" Plate
1/4" Bars welded to plate to keep wood from slipping
1" Tee
2 Angles 1½"x1½" welded into channel
3" Channel
Channel bolted to frame at 2 places
3" Channel
1½" Angle
1"x 3/16" Strap
1½" Angles
Chevrolet Valve Guides for Rollers (2 on each side)
Box Section made from 3" Channel
¾" Pipe Roller
Spring

Sketch and Data Courtesy Lincoln Electric Co.

the AMERICAN WELDING SOCIETY

activities • related events

Jennings Elected President

Charles H. Jennings, was born in Des Moines, Iowa, and attended Iowa State College at Ames, where he received the Degree of Bachelor of Science in Mechanical Engineering in 1928.

Mr. Jennings was admitted to the Graduate Student Course of the Westinghouse Electric Corp. in 1928, and completed the normal year's course in seven months.

In 1929 he became attached to the Mechanics Division of the Westinghouse Research Laboratories in East Pittsburgh, where he instituted advanced studies in the mechanical properties of welds, welding design, welding applications and welding problems.

Charles H. Jennings

In 1936 he was transferred to the Laboratories' Chemical and Metallurgical Dept. and put in charge of all the welding research. In 1943 he was made Section Engineer of the Welding Section and in 1945 promoted to Engineering Manager of the Welding Dept., which position he now holds.

In 1937, Mr. Jennings was called to England for three months as consultant welding engineer for the English Electric Co. He has also conducted a course in welding metallurgy at the Carnegie Institute of Technology, prepared a training course for welding operators for the Pittsburgh Board of Education and served as part-time staff member of the N.D.R.C. War Metallurgy Division during 1942. He is the author of more than thirty articles on welding and two books, *How to Weld 29 Metals* and *50 Lessons in Arc Welding.*

In 1939, Mr. Jennings was awarded the Westinghouse Silver "W" and Order of Merit for distinguished service in welding research and applications and was the 1950 Adams Lecturer. Mr. Jennings has been active in the AMERICAN WELDING SOCIETY for the past twenty years and has held the offices of Chairman, Pittsburgh Section, and District Vice-President, Director at Large and Director, Niagara Frontier Section. He was Chairman of the Technical Activities Committee and a member of seven other committees. He has served on six other committees, assisted in the preparation of several chapters for the 1938 and 1942 editions of the *Welding Handbook*, served as a member of the 1950 Handbook Committee and lectured before thirty-eight sections of the SOCIETY. He served as 1st Vice-President and 2nd Vice-President of the SOCIETY.

1st Vice-President
FRED L. PLUMMER

Fred L. Plummer was born in 1901. He holds a Bachelor of Arts degree from Ohio University and a Bachelor of Science in Civil Engineering from Case School of Applied Science and a Master of Science degree from the same school. He is a Registered Engineer of the states of Ohio

Fred L. Plummer

and New York. Mr. Plummer has written two books: one, on *Statically Indeterminate Structures* and the other on *Soil Mechanics and Foundations.* He is author of more than thirty articles published in various technical journals of which two on Pressure Vessels were published in THE WELDING JOURNAL and *The Oil and Gas Journal.*

Mr. Plummer was an Instructor and Associate Professor at Case for fourteen years from 1923 to 1937, teaching such subjects as Mathematics, Civil Engineering and Structural Engineering. He was a Lecturer during this period at Cleveland School of Architecture. He also lectured at John Huntington Polytechnic Institute.

In 1928, while on leave from Case, he was in charge of the structural analysis and design of the world's largest Airship Dock for Goodyear Zeppelin Corp. at Akron, Ohio, in association with Wilbur Watson & Associates, Consulting Engineers and Architects. He was also in charge of design of a seven million dollar steel bridge, being associated with Wilbur J. Watson and F. R. Walker.

During his stay with Case he was also consultant for a number of companies, such as the Truscon Steel Co., Republic Steel Co., Dow Chemical Co., U. S. Engineers Corps and others. From 1937 to 1940 Mr. Plummer was Chief Design Engineer of the Main Avenue Bridge Projects, Cleveland, Ohio. The project included the main bridge structure about one mile long with complicated approach structures. It was necessary to partially wreck and remodel many buildings, move and reconstruct many sewer and utility lines. The complete project cost about eight million dollars.

Since 1940 to date Mr. Plummer has been Chief Engineer and now Director of Engineering of the Hammond Iron Works, Warren, Pa. The company fabricates and constructs all types of shop- and field-erected steel plate structures and vessels used by the chemical, rubber and petroleum industries.

Mr. Plummer is a member of the following scientific and engineering societies: American Society of Mechanical Engineers, American Society of Civil Engineers, American Society for Testing Materials, American Petroleum Institute, American Concrete Institute, AMERICAN WELDING SOCIETY, Highway Research Board, American Society for Engineering Education, Cleveland Engineering Society, Welding Research Council, Society of Sigma Xi, Tau Beta Pi and Phi Beta Kappa.

In connection with the AMERICAN WELDING SOCIETY he was Director from 1941 to 1944; Chairman of the Cleveland Section from 1938 to 1941 and 2nd Vice-President 1950–51. He has served on the Committee on Awards and on the Handbook Committee. He is also a member of the Pressure Vessel Research Committee of the Welding Research Council.

WELDING

CODES STANDARDS and SPECIFICATIONS

Are you fully informed on the latest welding standards available? Have you missed any of the earlier standards? This column is published as a regularly monthly feature of The Welding Journal to keep you abreast of AWS technical standards, which are universally recognized as the most authoritative source of welding information.

The list of publications shown below is only partial; it is changed from month to month.

Keep informed—read this column regularly

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Buildings, Bridges, Tanks, Aircraft, Automotive, Pipe, Ships

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D3.2-44T Structural Failures in Welded Ship Construction (Tentative)	**25c**
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2nd Vice-President
ERIC R. SEABLOOM

Eric R. Seabloom was born in Sweden and came to the United States in 1909. He attended the Lane Technical High School and the Illinois Institute of Technology. In December 1917 he entered the

employ of Crane Co., Chicago, Ill., as a draftsman in the Product Engineering Dept., later serving as an estimator and designer. In 1921 he was transferred to the company's Engineering Laboratories as a testing engineer and did development work on industrial products. From 1925 to 1935 he served as Research Engineer in charge of the industrial Section of the Laboratories.

During this latter period his interest in welding grew and in 1932 he was given the position of Welding Engineer in addition to his other duties. The rapid growth of welding necessitated relinquishing his many assignments to devote full time to welding and welding research. In 1939 he was appointed Supervising Engineer of the Engineering Laboratories to coordinate the activities of the many laboratory groups. Since 1948 he has been Supervisor of Field Engineering. Welding still continues as his major interest.

Mr. Seabloom has been active in the AMERICAN WELDING SOCIETY for many years, serving as a Director-at-Large 1943 to 1946, Vice-Chairman 1941 to 1942 and Chairman 1942 to 1943 of the Chicago Section. He is a member of the A.W.S. Committee on Awards, A.W.S.-A.S.T.M. Committee on Filler Metals, W.R.C. Advisory Committee on Public Utilities Welding Research and has served on the Welding Handbook Committee, A.S.A. Code for Pressure Piping, Technical Committee of the Pipe Fabrication Institute and other committees.

Mr. Seabloom has published many papers on welding. He was co-author of Chapter 30 on "Chromium Iron and Steels" in the *Welding Handbook* and author on the chapter on Welding of Alloy Steels in the *H.P.C.N.A. Manual on Pipe Welding*. He has presented numerous talks, mostly on welding, before A.W.S. local sections and other engineering groups.

His technical society affiliations, beside the AMERICAN WELDING SOCIETY, include American Society for Metals, American Petroleum Institute and Society for Non-Destructive Testing.

DIRECTORS-AT-LARGE

Robert S. Green

Robert S. Green is associate professor and chairman of the Department of Welding Engineering at Ohio State University. Since he joined the University staff in 1947, welding engineering has been established as a separate degree-granting department of the College of Engineering.

A native of Lafayette, Ind., Professor Green received the degrees of bachelor of science in civil engineering in 1936 and master of science in engineering in 1942, both from Purdue University.

Before coming to Ohio State, he served in various capacities with several industrial organizations, including the American Bridge Co., Sinclair Refining Co., Carnegie-Illinois Steel Co., and as structural engineer with the Special Engineering Div. of the Panama Canal, resident engineer with the Power Piping Div. of the Blaw-Knox Corp. and structural engineer for the Weirton Steel Co.

He was instructor in general engineering at Purdue University from 1938 to 1940, and from 1943 to 1946 served in the Pacific area as an officer in the naval reserve.

He has been active in the affairs of the AMERICAN WELDING SOCIETY as chairman of the Columbus Section, member of the National Program, Convention, Standard Qualifications and Piping and Tubing Committees, and is chairman of the educational committee.

Professor Green is currently consultant to the National Certified Pipe Welding Bureau and is the A.W.S. sustaining member representative of that organization. He served as editor of the recent book, *Design for Welding*, published by the James F. Lincoln Arc Welding Foundation, Cleveland.

Irving A. Oehler

I. A. Oehler was born in Buffalo, N. Y., July 11, 1911. He received the following degrees from Rensselaer Polytechnic Institute: E.E. in 1932, M.E.E. in 1933, and D.E.E. in 1935.

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1/8"	dia. —	70 - 110 amps.
5/32"	dia. —	110 - 150 amps.
3/16"	dia. —	140 - 190 amps.

Properties of welds:

Joint	Tensile P. S. I.	% Elong.
Monel to mild steel	70,100	45
Nickel to mild steel	64,600	49
Monel to Type 316 stainless	87,800	44
Monel to Type 347 stainless	81,100	48

Guided bend test specimen of welded overlay made with new "140" Monel electrode directly on 3" by 6" by 3/8" steel plate. Note the excellent ductility and adherence of the deposit.

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I. A. Oehler

He was employed by the Republic Steel Corp., Buffalo District, 1935–42, becoming Assistant Chief Metallurgist in 1940. He joined the American Welding & Manufacturing Co. in 1942 and is now Director of Metallurgy & Research with that organization.

Dr. Oehler is a member of the AMERICAN WELDING SOCIETY, American Society for Metals, Society of Automotive Engineers, American Society for Testing Materials, American Association for Advancement of Science, National Society of Professional Engineers, American Ordnance Assn. and Sigma Xi.

He is Past-Chairman, Mahoning Valley Section, A.W.S. and Past-Chairman, Warren Chapter, American Society for Metals.

Michael S. Shane

M. S. Shane, a native Clevelander, was born Aug. 20, 1905. He attended Case School of Applied Science where he received his bachelor of science degree in Electrical Engineering in 1928. "Mike" went on to Carnegie Institute of Technology for advanced study of electrical and structural engineering, and received his master of science degree there in 1931. He remained at Carnegie Tech for the following 6 years as an instructor in electrical engineering.

Mr. Shane spent 1938 and 1939 in Pittsburgh and then Detroit as sales engineer with the Lincoln Electric Co. He joined the Cleveland Electric Illuminating Co. in 1940 to make special studies for the Chief Electrical Engineer. This work was interrupted in 1942, when he was granted a leave of absence to work in Columbia University's laboratory in New London, Conn., on underwater sound detection for the National Defense Research Council. Mike returned to the Illuminating Co. to head the new Technical Studies Dept. As Manager he directs rate and cost analyses and other special studies.

Mike has long been an active booster of

Michael S. Shane

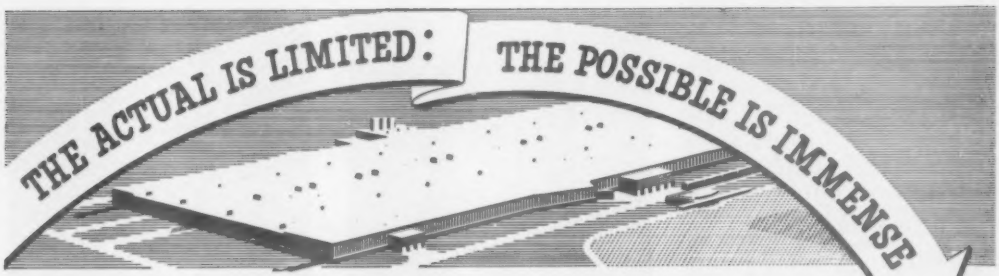
the AMERICAN WELDING SOCIETY. He has served 2 years as Cleveland Section Chairman, 1947 to 1949. He has become well known through his work in the WELDING SOCIETY and his work in 1949 as Chairman of the National Convention Committee.

Mike is an honorary member of Eta Kappa Nu and Tau Beta Pi and participatesactively in the Cleveland Engineering Society and American Institute of Electrical Engineers. He is a member of the Industrial Welding Committee of A.I.E.E. Currently he teaches Electrical Engineering courses in the Case Institute of Technology night school in the capacity of Associate Professor.

Edwin O. Williams

Edwin O. Williams was born in New Haven, Conn., in 1904, moving to the West Coast shortly thereafter. He considers himself practically a native Californian because he has lived in that state and attended schools in the coastal cities. He graduated from the University of Southern California with a degree of

E. O. Williams



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With welded steel, material costs are less. Only 50% to 25% as much metal is required. And, pound for pound, steel costs 75% less, giving an initial 7-to-1 cost benefit with which to start your designs.

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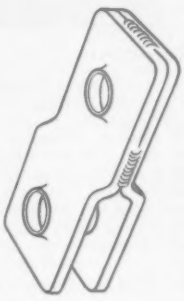

Fig. 5—**Steel Stamping** *from light gauge metal is formed in a die and welded. Weight is cut to 2 pounds . . . cost to 22¢ . . . a saving of 63% over Fig. 2. Saves 65% over Fig. 1.*

Fig. 4—**Saves 16% Cost** *over Fig. 2 by replacing tubing with simple "U" formed from bent steel strap. Holes are prepunched prior to welding. Weighs 2½ pounds . . . costs 49¢.*

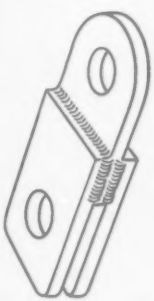

Fig. 3—**Uses Sheared Steel Strip** *to cut 10% from production costs, as compared with Fig. 2. Requires only simplest of fabricating equipment. Weight is 3 pounds . . . costs 54¢.*

the **ACTUAL**

Fig. 1—**Original Design.** *Machine clevis weighs 7 pounds . . . requires drilling, boring, reaming and spotfacing operations. Costs $1.45 for material and labor.*

increasing the YIELD

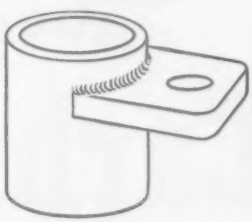

Fig. 2—**Welded Design Saves 60% Cost.** *Fabricated from tubing and steel strap. Needs only fast, simple punching operation. Weighs 3¼ pounds . . . costs only 58¢.*

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SEE HOW STEEL DESIGN CUTS COSTS STILL FURTHER

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THE LINCOLN ELECTRIC COMPANY

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bachelor of science in 1928. With the exception of 2 years spent with the Stoody Co. hard-facing specialists, his career since leaving school has been with the Victor Equipment Co. Starting in the Office and Shipping Dept. in 1930 he has been identified with the rapid growth of welding and Victor Equipment Co. in Southern California. He was promoted to Vice-President in 1945 and elected to the Board of Directors of Victor Equipment Co. in 1951.

He has been very actively identified with the AMERICAN WELDING SOCIETY activities, holding the position of Secretary-Treasurer for 7 years and Chairman in 1948–49 and many other assignments on committees. He assisted in the starting of the Phoenix Section and is very much interested in the work and progress of all phases of the AMERICAN WELDING SOCIETY.

NEWLY ELECTED DISTRICT VICE-PRESIDENTS

District 1—E. N. Adams

Ernest Adams is a District Engineer of the American Institute of Steel Construction. He graduated from the Worcester Polytechnic Institute and is a Licensed Professional Engineer in several states. Mr. Adams is a Past-Chairman of the Worcester Section of the AMERICAN WELDING SOCIETY and is a member of the American Society of Civil Engineers, Worcester Society of Civil Engineers and of the New England Building Officials Conference. He is also a representative of A.I.S.C. in the Building Officials Conference of America.

Ernest N. Adams

As District Engineer of the American Institute of Steel Construction, Mr. Adams regularly travels throughout New England and New York State, and it is therefore anticipated that he would have the occasion to visit with the various Sections located in this District.

District 3—J. U. Durham

J. U. Durham graduated from the University of Michigan in 1929 with the degree of bachelor of science in Engineering (Naval Architecture & Marine Engineering). He was employed by the Dravo Corp. from 1929 to 1935. In 1932 Mr. Durham attended the Schenectady Works of the General Electric Corp. to learn manual arc welding and study design of welded structure under Andrew Vogel. Subsequently he worked in the drafting room, shop and shipyard on first all-welded barges built by Dravo. Mr. Durham joined the American Bureau of Shipping in 1937 as surveyor to the Pittsburgh Office, and was transferred to

J. U. Durham

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the Nashville Office as Principal Surveyor in 1938, a position which he still holds. He was Chairman of the Nashville Section, A.W.S., in 1949.

District 5—T. B. Jefferson

Mr. Jefferson is a graduate mechanical engineer from the University of Kansas and for the past twenty years has been closely associated with welding. Upon leaving school, he became associated with a small structural steel and ornamental iron plant in Kansas City, Mo. In 1933, Mr. Jefferson became Assistant Plant Engineer with the U. S. Army Engineers on the $120,000,000 Fort Peck, Mont., dam project. In that capacity he was in charge of the engineer's shops which at that time included the world's largest maintenance welding shop. In 1939 he joined the office of Chief of Engineers, U. S. Army in Washington, D. C., as a welding engineer responsible for the design of welded dredging equipment.

T. B. Jefferson

Since 1940 Mr. Jefferson has been editor of the *Welding Engineer*, a McGraw-Hill publication, and the *Welding Encyclopedia*. He is a National Director of the AMERICAN WELDING SOCIETY and registered professional engineer of the State of Illinois. Mr. Jefferson is author of several books on welding and design subjects.

District No. 7—A. P. Maradudin

Alexci P. Maradudin, newly elected Vice-President of District No. 7, was born in Russia where his high-school education was completed. He came to the United States in 1923 and entered the University of Redlands, graduating in 1926 with a bachelor degree in Physics. Took graduate work at Stanford University and received his engineer's degree in Metallurgical Engineering. Since 1930 he has been employed by the Standard Oil Co. of California as Metallurgist and Materials Engineer. In this capacity he is in very close contact with welding and metallurgical problems in the refineries and the producing fields.

A. P. Maradudin

He has contributed papers for publication in various technical magazines, is a frequent speaker before technical societies and was Consulting Metallurgist on construction of the "Biggest Inch" 30-in. high-pressure Texas gas line from Blythe, Calif. to Los Angeles, Calif. and the Los Angeles Loop Line for the Southern California Gas Co. and the Southern Counties Gas Co.

He is a past-chairman of the Los Angeles Section of the AMERICAN WELDING SOCIETY, a member of the American Society for Metals and a member of Sigma Xi.

LIL CHAMP
ALRIGHT, JOE, LET'S FACE IT! WE'RE BACK IN IT AGAIN AN' EVERY MINUTE COUNTS! WHAT CAN WE DO TO START THE BALL ROLLING—PRODUCTION CONTROL SAYS WE HAVE ALL THE MEN AN' MATERIALS FOR TOP OUTPUT, YET WE'RE BEHIND SCHEDULE. WHAT GIVES!!!
OFFICE
IT'S FINAL ASSEMBLY IN THE WELDING DEPARTMENT- WE CAN'T GET ALL THE WORK OUT AN' MAKE REPAIRS ON REJECTS AT THE SAME TIME!
BOYS, THOSE CHAMPION GRAY DEVIL No.2 ELECTRODES DID THE TRICK! PRODUCTION IN OTHER DEPARTMENTS MUST INCREASE JUST TO KEEP THE WELDING DEPARTMENT ROLLING!
WELDING IS NO LONGER OUR BOTTLENECK- NOT SINCE WE PUT CHAMPION GRAY DEVIL No. 2 ELECTRODES TO WORK FOR US!
CHAMPION
WHEN SMOOTH BEADS, LOW SPATTER AN' 6000 PRODUCTION RATES ARE NECESSARY THAT'S WHEN YOU NEED CHAMPION GRAY DEVIL No.2. ELECTRODES! EXCELLENT ARC-STRIKING CHARACTERISTICS AND ITS ABILITY TO OPERATE ON HIGH WELDING CURRENTS MEANS FASTER WELDING AT LOWER OPERATING COSTS. WHEN WELDING—USE THE BEST—USE CHAMPION GRAY DEVIL No.2!!
THE CHAMPION RIVET CO
CLEVELAND, OHIO
East Chicago, Ind.

Corrosion Resistance of Metallized Steel

The AMERICAN WELDING SOCIETY'S Committee on Metallizing has organized a test program to determine the corrosion resistance of steel having metallized coatings of zinc and aluminum under exposure to different atmospheres.

Duplicate steel specimens will be sprayed with aluminum and zinc coatings varying in thickness from 0.003 to 0.015 in. with and without vinyl cover coats.

Preparation of the more than 4000 specimens to be exposed is already under way. When the specimens are completed early this Fall, they will be assembled for exposure at such standard corrosion test sites as those located at Kure Beach, N. C.; Point Reyes, Calif.; Gulf Coast; New York City and Wrightsville Beach, N. C. Exposure will be for periods of 1, 3, 6 and 12 years.

The test program is intended to provide authoritative data on what thickness of zinc or aluminum to use for different exposures for various lengths of time. The types of exposure include sea water, marine atmosphere and different industrial atmospheres.

Arrangements will be made for examination of exposed specimens at regular intervals and for issuance of periodic reports. Anyone interested in securing a copy of the program or progress reports when issued may write to Secretary, A.W.S. Committee on Metallizing, AMERICAN WELDING SOCIETY, 33 W. 39th St., New York 18, N. Y.

Meeting District #6

Some 34 delegates from Dallas, Denver, Houston, Oklahoma City, St. Louis, Tulsa and Wichita met at Forrest Park Hotel, St. Louis Friday and Saturday July 27th and 28th. The group were welcomed by Allen Wisler, District Vice-President. Present also was the National President Harry Pierce who talked on national activities and cooperation with the local Sections.

Reports of past activities and plans of each Section for next year were reviewed. Delegates enjoyed the meetings, entertainment and social events.

Symposium on Spot Welding Aluminum Alloys

The Dallas Section is sponsoring a symposium on the Spot Welding of Aluminum Alloys in Aircraft Construction, tentatively planned for Thursday and Friday, September 20th and 21st, for the benefit of aircraft plants in the Southwest.

This is to be a "shirt-sleeve" discussion of the everyday problems of the aircraft fabricator with the view of mutually assisting one another through the free interchange of viewpoints and experience.

The main subject headings planned for the meeting are: Specifications, Schedules and Welding Techniques, Inspection, Tip Pickup, Work Surface Preparation, Machines, Electrode Tips, Instrumentation, and Training of Personnel.

The fabricators expected to have representatives at the meeting are: Beech-Douglas, Convair, Chance Vought, Cessna, U. S. Air Force, McDonnell, U. S. Navy, Boeing, Texas Engineering & Mfg. Co. and Intercontinental Aircraft.

The meetings will be held at the plant of Chance Vought Aircraft in Grand Prairie, near Dallas, which plant is acting as co-sponsor of the meeting.

Jesse J. Brown is General Chairman of the meeting and can be addressed c/o Plant Engineering Dept., Chance Vought Aircraft, Box 5907, Dallas, Tex.

Recent Sustaining Members

Birdsboro Steel Foundry & Machine Co., Birdsboro, Pa. Designers and builders of steel mill equipment and auxiliary equipment; hydraulic machinery; Buchanan rock and ore crushers; carbon and alloy steel castings; iron and alloy iron and steel rolls; maintaining large foundry capacity for producing steel castings, machine shops with facilities for heavy machine work, and pattern shops. Specialists in cast-weld design and steel fabrications. A. L. Wentzel—Sustaining (A) Member Representative.

Ryan Aeronautical Co., San Diego 12, Calif. The engineering services and production facilities of the Ryan Aeronautical Co. are available in two divisions: (1) Airplane Div.—complete aircraft, pilotless target planes, missiles, large aircraft components, fuel tanks and aircraft structures; (2) Metal Products Division—jet engine components, rocket motors, heat exchangers, anti-icing equipment, complete exhaust systems for military and commercial aircraft and other high-temperature structures from corrosion-resistant steels and alloys. J. R. Fullerton—Sustaining (A) Member Representative.

Whitehead Metal Products Co., Inc., 303 W. 10th St., New York 14, N. Y. Distributors of the corrosion-resisting metals—aluminum, brass, bronze, copper, Everdur, Monel, nickel, Inconel and stainless steels. A network of seven Whitehead Warehouses blankets the northeastern part of the U. S., carrying in stock items of sheet, rod, pipe, tube, valves, fasteners, welding and brazing rods and specialty items for the metal-using industries. Technical assistance is available, backed by the Research Departments of the leading metal producers, on problems involving resistance to corrosion. John T. Stewart—Sustaining (A) Member Representative.

New Sustaining Member

The SOCIETY is pleased to welcome Irving A. Oehler of American Welding and Manufacturing Co. as a new Sustaining Member. This company located in Warren, Ohio, specializes in the fabrication of circular welded products. All resistance and fusion-welding processes are used with ferrous and nonferrous materials. Complete metallurgical, research and engineering departments assist customers on new parts and redesigns. American Welding has pioneered in welding heat-resisting alloys. The "AMWELD" Building Products Division manufactures a standardized line of interior steel doors frames and closet units for residential, commercial and institutional use.

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SEE US AT THE 1951 METAL SHOW—DETROIT, OCT. 15-19 EXHIBIT SPACE C-117

NEWS OF THE INDUSTRY

Engineering, Marine & Welding Exhibition at Olympia, London, Aug. 30 to Sept. 13, 1951

The Engineering, Marine & Welding Exhibition, which this year incorporates the Foundry Trades Exhibition, was again the largest event of its kind in the world. The Exhibition was first held in 1906 and has taken place ever since in alternate years with the exception of the interruptions caused by the two World Wars. The exhibition was held at Olympia, London, from August 30th to September 13th, and as it occurred during the Festival of Britain celebrations, arrangements had been made to receive a record number of overseas visitors. The exhibition opened each week-day from 10 A.M. until 8 P.M.

Welding Clinic

In response to the enthusiastic reception of a three-day Welding Clinic held in Baltimore last April, the Whitehead Metal Products Co., Inc., has scheduled a more comprehensive clinic to be held in its Buffalo offices and warehouse on Wednesday, Thursday and Friday, September 26th, 27th and 28th.

The Baltimore Clinic was opened by Mayor D'Alesandro and attended by more than 1100 industrial executives and welders from eleven Eastern states. It ran through 27 hours of demonstrations and lectures on welding data "not in the textbooks," answering practical problems submitted by metal users.

Problems included metal samples submitted by visitors for welding at one of the 12 booths on the floor, with a choice of techniques covering gas, electric, inert gas, metal arc, induction, gas and air, and resistance welding.

The Baltimore Clinic was designed to place new welding and brazing developments within reach of every defense plant Eastern Seaboard and thus help speed up defense production. The same objectives are planned for the Buffalo meeting.

Demonstrating technicians will include representatives from Aluminum Company of America, American Brass Co., American Brake Shoe Co., Crucible Steel Co., Handy & Harman, Induction Heating Co., The International Nickel Co., Inc., and Lukens Steel Co.

Metal users desiring further information are requested to write to Whitehead Metal Products Co., Inc., 303 W. 10th St., New York 14, N. Y.

ANNUAL MEETING
A.W.S.
Detroit—Week of
OCTOBER 15th

Wilson to Address Congress

Defense Mobilizer Charles E. Wilson has accepted the invitation of the American Society for Metals, 7301 Euclid Ave., to be the major speaker at the closing session of the World Metallurgical Congress in Detroit on October 19th.

William H. Eisenman, the Society's Executive Secretary, said Wilson will speak on the strategic importance of world metal conservation and production to the interests of free world defense.

The Metallurgical Congress is sponsored by the American Society for Metals and will be the first world meeting of metal specialists and production chiefs. It will be the largest industrial gathering ever held. More than 35,000 persons are expected to attend.

The Wilson meeting will be staged in the Statler Hotel at 6. P.M. for foreign representatives and their American counterparts. Each individual of other nations attending the Congress has been assigned an American host. Eisenman estimated that about 800 persons are expected at the final dinner meeting.

Ampco Distributer

Ampco Metal, Inc., of Milwaukee, Wis., has announced the appointment of the R. J. Greer Equipment Co., 116 S. Salina St., Syracuse, N. Y., as distributor for the Ampco Weld line of Resistance Welding Electrodes. They will include the state of New York, except metropolitan New York, as their territory.

New Dayton Plant of G-E Distributor

The new Dayton branch of the Burdett Oxygen Co. of Cleveland will serve as distributors for a complete line of G-E welding equipment, according to a joint announcement by the Burdett Oxygen Co. and General Electric Co.

The new facilities, which will be used to manufacture approximately $2^1/_2$ million feet of oxygen per month, will stock about two thousand items for welding and cutting, including a complete line of General Electric a.-c. and d.-c. welders, 70 types of electrodes, and more than 100 accessory items. Future plans include construction of an additional building to house an acetylene generator.

In charge of Dayton operations is Karl Bird, former manager of Burdett's Akron warehouse. Mr. Bird received his initial experience as salesman at Akron, from which position he rose to manager there.

The new Dayton plant will furnish oxygen for the company's Columbus and Cincinnati warehouses as well as the area between these plants. Personnel to man the new plant has been recruited from the Dayton area.

World Metallurgical Congress

More than 100 of an expected 150 major American industries, research laboratories and experimental stations already have accepted the invitation of the American Society for Metals to play host to top metal scientists of other nations who will attend the World Metallurgical Congress here, October 14th to 19th.

The American Society for Metals is sponsoring the Congress and arranging the so-called "Study Tours" of industrial centers. The tours will travel primarily in the Northeastern United States from September 17th to October 13th.

This was disclosed recently by Walter E. Jominy, President of the American Society for Metals, and William H. Eisenman, Executive Secretary.

More than 200 key metal scientists—or "conferees," as they are called—from 21 Marshall Plan and other free nations have been selected for tours by the State Department and the Economic Cooperation Administration. Individual teams of the visitors will visit more than 60 cities in 13 states, viewing at first hand American production and conservation methods in the metal industry. Tour itineraries are being arranged by the American Society for Metals and World Congress officials.

The Study Tours will converge in Detroit for the World Metallurgical Congress, October 14th to 19th, and the concurrently held 33rd National Metals Congress and Exposition, both A.S.M.-sponsored projects. The Detroit affair will be the first world gathering of metal scientists and production chiefs and the largest industrial congress ever held anywhere. More than 40,000 persons are expected to register individually at the meeting.

Members of the Tours will disembark in New York on September 13th and be welcomed to America at a Waldorf-Astoria Hotel luncheon on September 17th. They will visit essential industries along the Eastern Seaboard en route to Washington where a meeting with President Truman on September 21st is anticipated. From there they will break into specialized groups for the tours across the nation.

The Tours comprise 15 to 30 conferees per team and are organized according to interest: (1) Steel making and refining. (2) Rolling and manufacturing of copper, aluminum, magnesium and their alloys.

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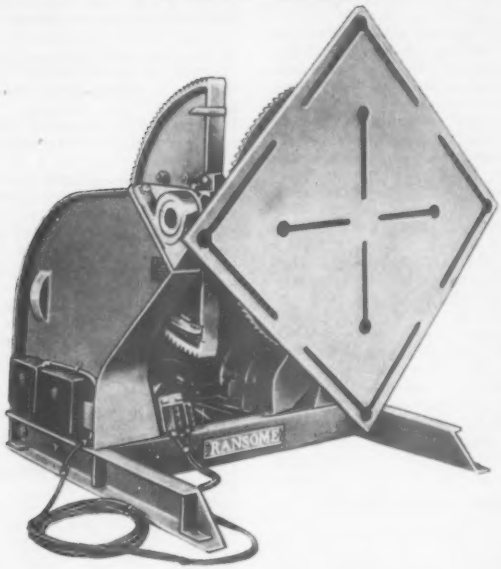

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Whether you produce "thousands alike" or "every one different", benefit from *increased arc-time* with Worthington-Ransome Welding Positioners. With just one set-up, the work is clamped to the table top. Then push-button controls or simple hand cranks do the work of tilting or rotating into any position convenient for economical downhand welding. Your welder can continuously weld without costly time and labor wasted in frequent rehandling of the work.

Result—up to 50% more footage, *better* welds (using higher current and heavier rods), less welding rod waste.

Welding positioner capacities from 100 lb to 30 tons. Also: turning rolls from 3 to 150 tons, stationary or self-propelled.

Write Worthington Pump and Machinery Corporation, Dunellen, New Jersey, for bulletins or additional information.

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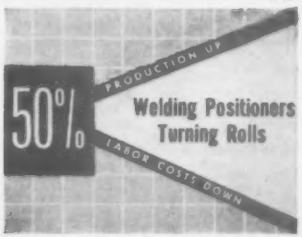

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(3) Metal fabrication; stamping, machining and finishing of alloys. (4) Heat treatment. (5) Welding and joining. (6) Inspection and testing. (7) A review of engineering and metallurgical education in the U. S. (8) A research tour.

Countries represented by conferees on the tours will be: Austria, Australia, Belgium, Brazil, Denmark, Finland, France, Germany, Greece, Holland, India, Italy, Japan, Luxembourg, Norway, New Zealand, Portugal, Sweden, Switzerland, Turkey and the United Kingdom.

The following corporations and organizations are among those open to Study Tours: United States Steel; Bethlehem Steel; Timken Roller Bearing; Ford Motor; American Brass; Dow Chemical; Pratt & Whitney; Cincinnati Milling Machine; Chrysler; International Harvester; General Motors; Rensselaer Polytechnic; Westinghouse; Pullman; Nash-Kelvinator; U. S. Navy Laboratories; Great Lakes Steel; Armour Research Foundation; Carnegie Institute of Technology; Massachusetts Institute of Technology; National Bureau of Standards, in Washington, D. C.; and universities of Harvard, Illinois and Columbia, among others.

Knolls Atomic Power Laboratory

Establishment of a Technical Division and an Operating Division in the Knolls Atomic Power Laboratory near Schenectady, N. Y., has been announced by Ralph J. Cordiner, president of the General Electric Co., which operates the laboratory for the Atomic Energy Commission.

In concurrent announcements by Dr. C. G. Suits, G-E vice-president and director of research, and Henry V. Erben, G-E executive vice-president, Dr. Kenneth H. Kingdon, of Schenectady, was named manager of the Technical Div., and William H. Milton, Jr., of Schenectady, manager of the Operating Div. Dr. Kingdon will be responsible to Dr. Suits, and Mr. Milton will be responsible to Mr. Erben.

Mr. Cordiner pointed out that the activities of the Knolls Atomic Power Lab. have progressed beyond its original research program to the point where they now also include extensive operating functions, necessitating establishment of the two component divisions.

The Technical Div., assigned to the Research Services Div., under direction of Dr. Suits, will be responsible for fundamental and applied research functions of the laboratory, Mr. Cordiner said.

The Operating Division, assigned to the Apparatus Group, under direction of Mr. Erben, will be responsible for operating functions, including engineering, manufacturing, legal and financial.

The Knolls Atomic Power Lab. was established in 1947 and occupies especially constructed, government-owned buildings on a site adjacent to the new quarters of the G-E Research Lab.

When first established, the laboratory's principal project was to design and

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Saxe System Welded Connection Units for welded assembly

Saxe Units place in position and securely hold together structural parts to be welded.
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ARC-DRIVE CONTROL for D-C Rectifier Welder

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...Reduced arc blow
...Completely adjustable by operator

Westinghouse RA Welders with new positive arc-drive control now prevent shorting when used on "drag" welding applications. In addition, they allow complete penetration on root passes of vertical and overhead welds. Arc-drive control is obtained by adjusting the ratio of short-circuit current to welding current without changing open-circuit voltage. Actual amount of arc-drive current can be varied by the operator.

For information on this improved RA Welder or other Westinghouse Welding Equipment, write Westinghouse Electric Corporation, Dept. DC70, Welding Division, Buffalo, New York. J-21607-A

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WELDING EQUIPMENT

build an experimental atomic power plant for peacetime use. However this project has been deferred in favor of one calling for building of an atomic power plant for the U. S. Navy for use on submarines.

Battelle Opens European Branch

Battelle Institute, world's largest independent industrial research foundation, will establish a European branch of its laboratories to serve the European economy, it was announced recently.

According to Battelle Director Clyde Williams, the decision follows a recently completed survey of scientific and industrial conditions in Europe.

Since 1929 Battelle, at its laboratories in Columbus, Ohio, has served American industry through science and technology. It plans now to serve European industry in the same way.

"The accumulated knowledge and experience gained through Battelle's service to American industry can do much to help industry in Europe increase its productivity and elevate the standards of living there," Dr. Williams said. "At the same time, European science has much to offer American scientists, especially in the way of fundamental knowledge and research. By working together, research men of both continents will benefit from association with each other. We believe further that the establishment of an international research center will do much to promote world peace by encouraging understanding and friendship among scientists of all nations. We look forward to this opportunity to increase our service to mankind throughout the world."

Exact location of the European branch and other details will depend upon the outcome of negotiations now in progress. It is anticipated, however, that the new service will be in operation early next year.

The Columbus laboratories of Battelle have a staff of 1600 scientists, technologists, and their assistants. This year they will conduct an estimated $9,000,000 worth of research for industry and government.

World Metallurgical Congress and Study Tours of U. S. Industry—September 13th to October 19th

Sponsored by the American Society for Metals, 7301 Euclid Ave., Cleveland, Ohio, the purpose of the Congress and Study Tours is to assess distribution and potential of world's metal resources available to the noncommunist world. This is the first world meeting in history of top-echelon industrialists dealing with steel, alloy and metal production.

Foreign "conferees" disembarked in New York on September 13th. Luncheon was served at the Starlight Roof of the Waldorf-Astoria on September 17th, special invited guest being Herbert Hoover. The Washington ceremony on September 21st called upon President Truman at the White House to "lead production-for-defense in the Free World." The World Metallurgical Congress will be staged in Detroit, October 14th to 19th, concurrently with the 33rd National Metals Congress and Exposition.

Economic Cooperation Administration: State Department and Office of Defense Mobilization each has appointed a special liaison officer to handle Congress affairs. Study Tours and Congress are known as E.C.A. "special project 80." E.C.A. asks permission to allow three other of its special assistance groups to participate in Congress. *Beware:* The project is for exchange of ideas on metal development in production. It is *not* to exchange *information* vital to national defense. Foreign representatives are known as "conferees," not as delegates, though many do come fully accredited by foreign metal-producing groups.

On the Study Tours conferees are broken into eight groups according to interest. Before arriving in Detroit the groups will visit 51 cities in 12 states and the District of Columbia, to view biggest Americaft industries and methods.

American Bureau of Shipping Activities (Extracted from an Address of Walter Green, President A.B.S.)

Bureau Activity

There now exist in class with the American Bureau of Shipping 8390 vessels of 36,495,987 gross tons, of which about 20%

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General Electric

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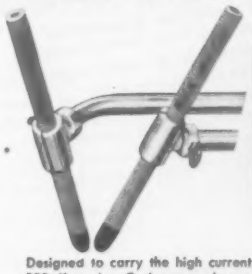

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are temporarily inactive, compared to 25% a year ago. To this figure there will be added 350 vessels of 1,890,946 gross tons currently under construction in United States and foreign shipyards, making a grand total of 8740 vessels of 38,386,933 gross tons. While a substantial percentage of these vessels are foreign owned and/or registered, the large majority fly the American Flag. These figures include seagoing tonnage, Great Lakes and river craft, both self-propelled and nonpropelled.

On July 1st of last year contracts were in existence for the construction of 217 new vessels to be built to Bureau Class, these totaling approximately 824,637 gross tons. At the same time this year, there were under contract to be built to Bureau Class 350 vessels of approximately 1,890,946 gross tons, an increase of 130%.

Foreign Business

The Bureau's business in foreign ports has been showing a continuing steady increase. Since the January 1951 Annual Meeting, additional exclusive surveyors have been stationed in foreign ports, and arrangements were made to open exclusive offices at Beirut, Lebanon, and Essen, Germany; the latter for testing materials. A number of additional nonexclusive surveyors have also been appointed in foreign countries.

Construction of new vessels to Bureau Class in foreign countries has increased since the first of the year. A total of 111 vessels are being built to Bureau Class in England, Scotland, Belgium, Holland, Germany, Canada, Mexico, Spain, Argentina, Brazil, Pakistan, Japan, Italy and Trieste, where staffs of exclusive surveyors are maintained.

In England and Scotland 17 large vessels are being constructed to American Bureau of Shipping Class. Of these, 13 are oil tankers and 4 are cargo vessels. Additional new vessels for Bureau Class are in prospect here.

In connection with Germany's newly revived shipbuilding industry, 2 cargo ships and 5 tankers are being built to Bureau Class. One of the tankers will be the largest in the world, measuring 722 ft. in length between perpendiculars and being of 40,000 tons dead weight. Additional new work is also in prospect here for the Bureau.

Thirty-seven cargo vessels and three tankers are being built to Bureau Class in Japan. Most of these vessels are for Japanese ship owners, but a few are for export.

In Italy and Trieste 11 large combination passenger and cargo vessels, one tanker, two cargo ships, a cable-laying vessel and a whale factory ship 639 ft. long are being constructed to Bureau Class; these being for Italian and Greek owners.

In Belgium, Spain, Holland, Canada, Mexico, Argentina, Brazil and Pakistan, 31 vessels are being built to Bureau Class.

A number of additional existing foreign-flag and -owned vessels have recently been classed by the Bureau.

ANNUAL MEETING
A.W.S
Detroit — Week of
OCTOBER 15th

Welding Outlet Tunnel

Special welding "Jumbo," complete with four General Electric type WD-43 d.-c. arc welders, is rolled into place to weld another section in the 1138-ft. all-steel outlet tunnel of the new Lucky Peak Dam, under construction on the Boise River.

Fabricated from $^3/_4$ and $^1/_2$-in. steel plate by the Olson Manufacturing Co., Boise, Idaho, the 23 ft. diameter pipe was built in 40-ft. sections at Olson's plant and hauled by a specially designed trailer 8 miles to the dam site.

In making the huge sections, three 10- x 24-ft. plates were rolled and welded into a single hoop 23 ft. in diameter. The company then submerged-melt welded four such hoops together to form each 40-ft. section. Each section was braced with 12 stiffener tees. So that only inside welding would be required in the field, and to make line-up of the sections in the tunnel easier, a butt-strap back-up plate was shop welded to the ends of each section, one-half strap to each end.

Louisiana Welding Supply Co. Moves in New Quarters

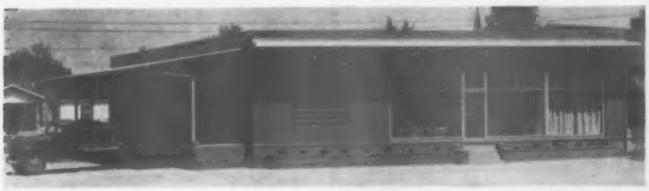
The Louisiana Welding Supply Co. of Baton Rouge, La., have recently moved into the new and larger quarters shown in the picture below. The building includes ample dock space, attractive display rooms, storage rooms and air-conditioned offices. The firm is owned jointly by C. O. Stilwell and Paul E. Haygood, both members of the AMERICAN WELDING SOCIETY.

Microwave Reflectors Welded

New miracles are constantly being performed in the world of communications. The latest is the use of microwave radio relays for transmission of as many as 14 messages on a single main signal. Essential for transmitting microwave signals which, like light waves, travel only in straight lines, are a series of relay stations, about 30 miles apart, each with towers 150 to 200 ft. high.

Fig. 1 This aluminum reflector screen frame for microwave transmission must be accurately adjusted, and then mounted on top of a microwave tower

The top of each tower carries two reflector screens or "mirrors," serving to reflect incoming waves to the relay or repeater station at the tower base, and to reflect the outgoing wave from the tower to the next relay station. The aluminum reflector screen must be very accurately adjusted to assure perfect transmission.



"Bronze-Facing" with Ampco® Electrodes Saves $4800

4-ton turbine runner "Bronze-Faced" with Ampco Electrodes.

Cavitation and erosion caused severe wear on the buckets and blades of this large 4-ton turbine runner. A new runner costs $6000. "Bronze-Facing" with Ampco Electrodes cost only $1200 — a savings of $4800!

Better Than New!

"Bronze-Facing" with Ampco Electrodes is used throughout industry to increase service life, provide superior bearing qualities, obtain improved resistance to wear and keep costs down . . . production up.

You too can save time and money by using Ampco Electrodes in your repair and maintenance operations. Get a supply today from your Ampco distributor, or write us.

Power company personnel preparing turbine runner for "Bronze-Facing" with Ampco Electrodes.

W-154

AMPCO

Ampco Metal, Inc.
Milwaukee 46, Wis.

West Coast Plant, Burbank, California

It's Production-Wise to Ampco-ize!

®Reg. U. S. Pat. Off.

EUTECTIC *Brings You Revolutionary*

New Ways to Join All Metals.

Unbelievable savings in metal-joining can now be yours through the use of "Low Temperature Welding Alloys"® discovered a few years ago and now used in over 78,000 industrial plants throughout America for more efficient metalworking production as well as for salvaging irreplaceable tool and machine parts.

Over 100 different, new, EUTECTIC Low Temperature WELDING ALLOYS® and EUTECTOR® Fluxes are job-engineered for use on ALL metals—cast iron, alloy steels, aluminum, copper and nickel alloys, die castings, overlays, etc., and may be applied with ALL heating methods — torch, arc, furnace, induction, etc.

In metal-joining, the "proof of the pudding" is in the demonstration, NOT in the advertising claims. That is why we offer a FREE Consultation-Demonstration right in your own shop with your own personnel . . . without cost or obligation on your part in any way.

EUTECTIC's "Manual of Welding Engineering and Design"—48 pp. jam-packed with factual data, charts, specifications, how-to-do-it data, etc.,—now in its third giant printing—yours FREE for the asking!

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Without cost or obligation, now or later—

☐ Send me a FREE copy of your new "Manual of Welding Engineering and Design."

☐ Send your local District Engineer to conduct a FREE Consultation-Demonstration in my plant. (Have him phone for appointment, first.)

Signed_____
Firm_____
Address_____ Zone_____
City_____ State_____

Heliare welding proved to be the answer in the fabrication of reflector frames at Pioneer Iron Works, Sioux City, Iowa. Heliare welding equipment was provided by The Linde Air Products Co., a Division of Union Carbide and Carbon Corp.

The reflector frame shown in the accompanying photograph is one of many being installed by the Tower Construction Co. of Sioux City for use of an oil pipe-line company. Instant microwave transmission of varying pipe-line pressures at many points will facilitate the coordination of pumping equipment along a 1500-mile pipe-line system.

Induction Heating Film

A few of the thousands of uses to which induction heat is being put by industry today are shown in a new 16-mm. sound film in color released by Allis-Chalmers Mfg. Co.

Production scenes photographed are varied. They include heating of steel strip for baby buggy springs, annealing of automobile brake arms, brazing fittings into refrigerator compressor housings, and hardening eight localized areas of a rocker arm shaft.

These samples, showing the use of induction heating in industry, help to emphasize the effectiveness and speed of the method and demonstrate its versatility possible through the use of different work fixtures. A brief visual explanation of how induction heating operates is included in the film.

Running time of "Induction Heat" is 20 min. Prints of the film are available for group showing upon request from Allis-Chalmers Mfg. Co., Advertising and Industrial Press Dept., General Machinery Div.

Ceramic-Coated Parts

Ryan Aeronautical Co. recently announced the nation's first volume production contracts for adaptation of an ancient art to modern aircraft—ceramic coating of exhaust systems to conserve growingly scarce strategic alloys.

Approximately 600 sets for Pratt & Whitney engines, including spares, for the Convair Model 240 and Model 340 transports will be ceramic-coated, Sam C. Vreder, Ryan Customer Service Director, reported.

In addition, Boeing Airplane Co., Seattle, has ordered ceramic-coated Ryan exhaust assemblies for all its B-50 bombers and C-97 Stratofreighters under production for the air force.

In the jet engine field, General Electric has given Ryan experimental orders for ceramic coating of the transition liners and inner combustion chambers Ryan manufactures for the J-47 engine. G-E has launched a program of using nonstrategic materials on its famed J-47 engine, which will require ceramic coating for protection.

Experimental work also is being conducted with Douglas Aircraft Co. and United Air Lines on ceramic coating of DC-6 transport exhaust stack assemblies, through service tests on actual scheduled runs.

Imagine! Armor plate of any thickness

flame-cut to ± 1/16" Tolerance

ONLY THE ACETOGEN PROCESS OFFERS THESE STARTLING ADVANTAGES:

- Armor plate of all thicknesses flame-cut to ±1/16" tolerance!
- The flame-cut plates are ready for welding without grinding, finishing, bridging, or beading!
- Single- and multi-bevels are as easy to flame-cut as square-edges!
- Flame-cut plates have "feather" edges... surfaces have sufficiently low Brinell to allow machining!
- No heavy investment needed; the Acetogen Process utilizes standard, readily-available, low-cost equipment and tools!
- Experienced flame-cutters become qualified Acetogen operators within a few days!

AND THAT'S JUST ONE of the many amazing facts about the Acetogen Process—the only really new flame-cutting development in years!

The Acetogen Process now makes *complete* plate-edge preparation possible *with a flame!* It saves vital man-hours by eliminating most of the grinding, finishing, beading, and bridging formerly necessary. It makes *precision* flame-cutting a reality!

Acetogen Fabricators are proving these statements *today* in the Philadelphia plant of Henry Disston & Sons, Inc.—doing a job that industry thought couldn't be done. They are turning out thousands of armor plates—each one precisely cut, each one duplicating its pattern to within 1/16th of an inch! This work is a source of continual satisfaction at Disston, and Baldwin-Lima-Hamilton, where the plates are assembled.

We can produce results just as eye-opening in *your* plant. We have the gas and nozzles, and the personnel with the "know-how"; we will act either as consultants, or as sub-contractors doing the job in your plant. We invite your inquiry. Acetogen Fabricators, Inc., 822 Commercial Trust Bldg., Philadelphia 2, Pa.

Originators of precision flame-cutting

ACETOGEN FABRICATORS, INC.

TENTATIVE PROGRAM

AMERICAN WELDING SOCIETY

32nd Annual Meeting
week of October 15, 1951

HOTEL BOOK CADILLAC, DETROIT, MICH.

Welding and cutting demonstration will be featured at the National Metal Exposition held during the week at the Michigan State Fairgrounds

TECHNICAL SESSIONS—All Sessions on Ball Room Floor

All Technical Sessions will positively start on time.

No Stenotype Reporter—Members desiring to discuss papers are urgently requested to prepare discussion in writing in advance of the meeting and to send copies to Headquarters, as those preparing written discussion will be given preference at the sessions. Members and guests giving extemporaneous discussion at meeting should forward a written discussion as soon as possible after the meeting.

MONDAY MORNING, OCTOBER 15th—9:30 A.M.

Two Simultaneous Sessions

1—STRUCTURAL WELDING

Crystal Ball Room

Chairman—La MOTTE GROVER
Air Reduction Sales Co.

Co-Chairman—MARTIN P. KORN
Consulting Engineer

Yield Strength of Welded Continuous Beams
by C. H. YANG and L. S. BEEDLE, Lehigh University, Fritz Engineering Lab., and H. G. JOHNSTON, University of Michigan

Column Strength Under Combined Bending and Thrust
by R. L. KETTER and L. S. BEEDLE, Lehigh University, Fritz Engineering Lab., and B. G. JOHNSTON, University of Michigan

Estimating Weldments and Welded Structural Steel
by CHAS. F. FRANTZ, Lehigh Structural Steel Co.

Surface Conditioning of Structural Steel by Welding
by R. E. SOMERS and H. C. VON BLOHN, Bethlehem Steel Co.

2—RESISTANCE WELDING

Italian Garden

Chairman—JACK OGDEN
Fisher Body Div., General Motors Co.

Co-Chairman—B. L. WISE
National Electric Welding Machine Co.

Physical and Metallurgical Characteristics of Spot Welding Titanium
by M. L. BEGEMAN, J. C. FONTANA and FRANK W. MCBEE, JR., The University of Texas

The Application of Spot and Seam-Welding to Design
by S. P. JENKINS and THOMAS E. PIPER, Northrop Aircraft, Inc.

Spot and Projection Welding Using Magnetic Electrode Force
by WILLIAM E. KLINGEMAN and H. H. KRUER, Precision Welder and Machine Co.

A Case of Power for Spot-Projection Welders
by MYRON ZUCKER, Myron Zucker Engineering Co., JERRY GERALDS, Midwest Wire Products Co., and PAUL DUKER, The Detroit Edison Co.

MONDAY AFTERNOON, OCTOBER 15th — 2:00 P.M.

Three Simultaneous Sessions

3—RESISTANCE WELDING

Italian Garden

Chairman—J. RANDALL
Ford Motor Car Co.

Co-Chairman—R. C. McMASTER
Battelle Memorial Institute

Seam Welding Containers Automatically
by C. S. SELTZER, Swift Electric Welder Co.

Spot and Seam Welding of Nimonic and Similar Heat-Resistant Alloys
by J. SOLOMON, Sciaky Bros., Inc.

Temperature Distribution During the Flash Welding of Steel
by ERNEST F. NIPPES, W. F. SAVAGE and J. J. McCARTHY, Rensselaer Polytechnic Institute

4—WELDABILITY

Grand Ball Room

Chairman—CLARENCE E. JACKSON
Union Carbide and Carbon Research Labs.

Co-Chairman—R. S. ARCHER
Climax Molybdenum Co.

Microcracks and the Low-Temperature Cooling Rate Embrittlement of Arc Welds in Mild Steel
by PROF. A. E. FLANIGAN, Dept. of Engineering, University of California

Effect of Sub-Critical Cooling Rate on Strain and Quench Aging of Structural Steels
by C. FELMLEY, C. HARTBOWER and W. S. PELLINI, Metallurgy Div., Naval Research Lab.

5—NONFERNOUS

Crystal Ball Room

Chairman—G. O. HOGLUND
Aluminum Company of America

Co-Chairman—I. C. MATTSON
Dow Chemical Co.

Tensile Tests and Metallurgical Studies of Welded Copper Joints
by R. J. MOSBORG, R. W. BOHL, F. L. HOWLAND and W. H. MUNSE, Dept. of Civil Engineering, University of Illinois

Welding Iron-Bearing Alpha Aluminum Bronze
by F. EMERY GARRIOTT, Weldrod Dept., Ampco Metal, Inc.

Pressure Welding Aluminum at Various Temperatures
by M. A. MILLER and G. W. OYLER, Aluminum Research Labs.

MONDAY EVENING, OCTOBER 15th — 6:00 P.M.

President's Reception

GRAND BALL ROOM

TUESDAY MORNING, OCTOBER 16th — 9:30 A.M.

Two Simultaneous Sessions

6—SHIP STRUCTURE

Crystal Ball Room

Chairman—CAPT. E. A. WRIGHT
U.S.N., Bureau of Ships

Co-Chairman—COMMANDER D. B. HENDERSON
U. S. Coast Guard

Work of the Ship Structure Committee
by REAR ADMIRAL K. K. COWART, U. S. Coast Guard

Low-Carbon Steel: Subcritical Heating vs. Transition Temperatures
by L. J. KLINGER, E. B. EVANESKES and WM. M. BALDWIN, Case Institute of Technology

Studies of Tests for Evaluating Welded Ship Steels
by C. B. VOLDRICH and P. J. RIEPPEL, Battelle Memorial Institute

Stress Studies of Bulkhead Intersections for Welded Tankers
by W. R. CAMPBELL, L. K. IRWIN and R. C. DUNCAN, National Bureau of Standards

The Influence of Composition and Steel-Making Practice upon Ship-Plate Quality
by H. M. BANTA, Battelle Memorial Institute

7—FUNDAMENTAL STUDIES OF ARC WELDING

Italian Garden

Chairman—W. B. KOUWENHOVEN
Johns Hopkins University

Co-Chairman—R. W. CLARK
General Electric Co.

The Effect of Power Supply Characteristics on D.-C. Welding
by JACK B. KEYTE, Dept. of Welding Engineering, Ohio State University

Welding Characteristics of Submerged Arc with Three-Phase Power
by E. A. CLAPP, Union Carbide and Carbon Research Labs., Inc., and NORMAN G. SCHREINER, Linde Air Products Co.

Tools for Predetermining Preheat and Interpass Temperatures for Submerged Arc Welds
by CLARENCE E. JACKSON and ARTHUR F. SHRUBSALL, Union Carbide and Carbon Research Labs., Inc.

TUESDAY MORNING, OCTOBER 16th

Inspection Trip

Arrangements have been made for an Inspection Trip to the plants of the Ford Motor Co.

TUESDAY AFTERNOON, OCTOBER 16th — 2:00 P.M.

Three Simultaneous Sessions

8—SHIP STRUCTURE

Crystal Ball Room

Chairman—DAVID P. BROWN
American Bureau of Shipping

Co-Chairman—FINN JONASSEN
National Research Council

Welded Reinforcement of Openings in Structural Steel Plates
by D. VASARHELYI and R. A. HECHTMAN, University of Washington

Evaluation of Welding Procedure by Direct Explosion Testing
by G. S. MIKHALAPOV, Metallurgical Research and Development Co.

Investigation of Factors Which Determine Welded Performance
by C. HARTBOWER and W. S. PELLINI, Naval Research Lab.

Upper and Lower Transition in Charpy Tests
by W.J. HARRIS, JR., J. A. RINEBOLT and R. RARING, Naval Research Lab.

9—HARD FACING AND FLAME HARDENING

Grand Ball Room

Chairman–E. V. DAVID
Air Reduction Sales Co.

Co-Chairman—L. W. BOELTER, Climax Manufacturing Co.

Control of Rail-End Hardening
by La MOTTE GROVER, Air Reduction Sales Co.

Hard Facing for Impact
by HOWARD S. AVERY, American Brake Shoe Co.

Development of Fused Metallized Coatings
by HARRISON S. SAYRE, U. S. Naval Engineering Experiment Station

10—RESISTANCE WELDING

Italian Garden

Chairman—F. R. HENSEL
P. R. Mallory & Co.

Co-Chairman—T. EMBURY JONES
Precision Welder & Machine Co.

Trends in Electronic Nonsynchronous Resistance Welding Controls
by STUART C. ROCKAFELLOW, Robatron Corp.

Flash Welding of Components for Aircraft and Similar Applications
by J. H. COOPER, The Taylor-Winfield Corp.

Fatigue Strength of Spot-Welded Light Alloy Joints
by H. KIHARA, President, The Japan Welding Society

EDUCATIONAL LECTURE SERIES —4:30 P.M. by W. R. PLUMMER, Progressive Welder Company

TUESDAY EVENING, OCTOBER 16th — 8:00 P.M. 1951 ADAMS LECTURE

The Welding of Copper by the Inert Gas Metal Arc Process by JOHN J. CHYLE, A. O. Smith Corp.—Crystal Ball Room

WEDNESDAY MORNING, OCTOBER 17th — 9:30 A.M.

Three Simultaneous Sessions

11—PRODUCTION WELDING

Crystal Ball Room

Chairman—A. E. LINDSEY
Ford Motor Co.

Co-Chairman—W. C. LANG
Chrysler Corp.

Welding Heat Exchanger for the Chemical Industry
by JOHN W. MORTIMER, Professional Engineer

Product Design for Welding
by JOHN MIKULAK, Worthington Pump and Machinery Corp.

Welding Fixtures for Use with Submerged Arc
by J. P. BERKELEY, Berkeley Equipment Co.

12—PRESSURE VESSELS

Grand Ball Room

Chairman—H. C. BOARDMAN
Chicago Bridge & Iron Co.
Co-Chairman—L. C. BIBBER
Carnegie-Illinois Steel

Effect of Plate Edge Preparation on Notch Toughness
by DR. S. S. TOR, E. I. du Pont de Nemours and Co.; J. M. RUZEK, DR. R. STOUT, Lehigh University

Biaxial Fatigue Tests on Flat Plate Specimens
by R. U. BLASER, L. F. KOOISTRA and J. T. TUCKER, JR., The Babcock & Wilcox Co.

Stresses in Cylindrical Pressure Vessels on Two Saddle Supports
by LEONARD P. ZICK, Chicago Bridge and Iron Co.

13—GAS CUTTING

Italian Garden

Chairman—G. V. SLOTTMAN
Air Reduction Sales Co.

Co-Chairman—H. E. ROCKEFELLER
Linde Air Products Co.

Oxygen Cutting of Defense Equipment Materials
by A. H. YOCH, Air Reduction Sales Co.

Heavy Scrap Cutting in the Steel Mill
by L. P. ELLY, Bethlehem Steel Co.

Powder-Washing for Metal Removal
by R. S. BABCOCK, Linde Air Products Co.

WEDNESDAY AFTERNOON, OCTOBER 17th — 2:00 P.M.

Three Simultaneous Sessions

14—WELDABILITY

Grand Ball Room

Chairman—J. LYELL WILSON
Consulting Naval Architect
Co-Chairman—J. W. HALLEY
Inland Steel Co.

The Relation of Notch Strains to Bend Angles in the Notched-Bend Test
by PROF. A. E. FLANIGAN, University of California, and ERNEST M. EMERY, North American Aviation Co.

Repeated Load Tests on Welded and Prestrained Steel
by DR. S. S. TOR, E. I. du Pont de Nemours and Co.; DR. J. M. RUZEK, R. D. STOUT, Lehigh University

The Micro-Mechanism of Fracture in the Tension-Impact Test
by W. H. BRUCKNER, University of Ill.

15—WELDING AND BRAZING

Crystal Ball Room

Chairman—A. N. KUGLER
Air Reduction Sales Co.

Co-Chairman—R. E. POWELL
Western Electric Co.

Nested Electrodes for Metal Arc Welding
by W. A. SNYDER, University of Washington

Welding in Steel Mill Maintenance During Defense Period
by R. L. DEILY, Air Reduction Sales Co.

Dilution and Diffusion Aspects of Brazing
by R. D. WASSERMAN and JOSEPH F. QUAAS, Eutectic Welding Alloys Corp.

16—STAINLESS STEELS

Italian Garden

Chairman—G. E. CLAUSSEN
Reid-Avery Co.

Co-Chairman—W. O. BINDER
Union Carbide and Carbon Research Labs.

Welding of High-Alloy Steel Castings
by R. D. THOMAS, JR., Chairman of WRC Committee on the subject

Welded Joints Between Dissimilar Metals for High-Temperature
by R. W. EMERSON, Pittsburgh Piping & Equipment Co.

Nitric Acid Corrosion Data of Welded Stainless Steels
by M. A. SCHEIL and H. F. EBLING, A. O. Smith Corporation

EDUCATIONAL LECTURE SERIES —4:30 P.M. by W. R. PLUMMER, Progressive Welder Company

WEDNESDAY EVENING, OCTOBER 17th — 7:30 P.M.

UNIVERSITY RESEARCH CONFERENCE

Founders Room

17—SYMPOSIUM ON WELDING OF JET ENGINE ALLOYS—8:00 P.M.

Crystal Ball Room

Chairman—A. E. LINDSEY, Ford Motor Company

Co-Chairman—WALTER GARMAN General Electric Company

Resistance and Inert Arc Welding of Jet Engine Alloys
by A. J. Rosenberg, General Electric Company

Brazing Jet Engine Alloys for High Temperature Service
by R. L. PEASLEE, Wall Colmonoy Corporation

Flash Butt Welding of High Temperature Alloys
by Dr. I. A. Oehler, American Welding and Manufacturing Co.

THURSDAY MORNING, OCTOBER 18th — 9:30 A.M.

Three Simultaneous Sessions

18—EDUCATIONAL

Crystal Ball Room

Chairman—R. S. GREEN
Ohio State University

Co-Chairman—J. HEUSCHKEL
Westinghouse Electric Corp.

Selecting and Training Welding Operators for the Defense Program
by A. N. KUGLER, Air Reduction Sales Co.

Metallurgy for the Welding Student
by J. D. PATERSON, Cass Technical High School

Tentative Standards for School Welding Shops
by CARL H. TURNQUIST, Cass Technical High School

Welding Instruction in the Public Schools
by A. D. ALTHOUSE, Detroit Public Schools

19—WELDABILITY

Italian Garden

Chairman—W. F. HESS
Rensselaer Polytechnic Institute

Co-Chairman—D. H. COREY
Detroit Edison Co.

The Arc Welding of Carbon-Molybdenum Steel Pipes
by F. J. WINSOR, E. I. du Pont de Nemours and Co.

Residual Stresses Due to Circumferential Welds in Seamless Mild Steel Pipe
by L. J. PRIVOZNIK, Standard Oil Co. (Ind.)

Heat Treating Properties of Low-Hydrogen Electrode Weld Metal
by D. C. SMITH and W. G. RINEHART, Harnischfeger Corp.

High-Temperature Welded Joints
by R. H. ENGLISH, National Alloy Steel Co.

20—INERT-ARC WELDING

Grand Ball Room

Chairman—A. J. ERLACHER
United Engineers & Constructors, Inc.

Co-Chairman—K. L. WALKER
Foster Wheeler Corp.

Inert Gas Shielded Metal Arc Welding of Magnesium
by PAUL KLAIN, Dow Chemical Co.

Aircomatic Welding of Ferrous Metals
by E. Di LIBERTI, Air Reduction Co.

Metal Transfer in Sigma Welding
by R. T. BREYMEIER, Union Carbide and Carbon Research Lab., Inc.

High-Speed Consumable Electrode Machine Welding for Aircraft
by BERNARD GROSS and R. A. SMITH Rohr Aircraft Corporation

THURSDAY AFTERNOON, OCTOBER 18th — 2:00 P.M.

Crystal Ball Room

SYMPOSIUM ON FILLER METAL SPECIFICATIONS FOR INERT-GAS AND SUBMERGED-ARC WELDING

Sponsored by AWS-ASTM Committee on Filler Metal

2:00 P.M.

Italian Garden

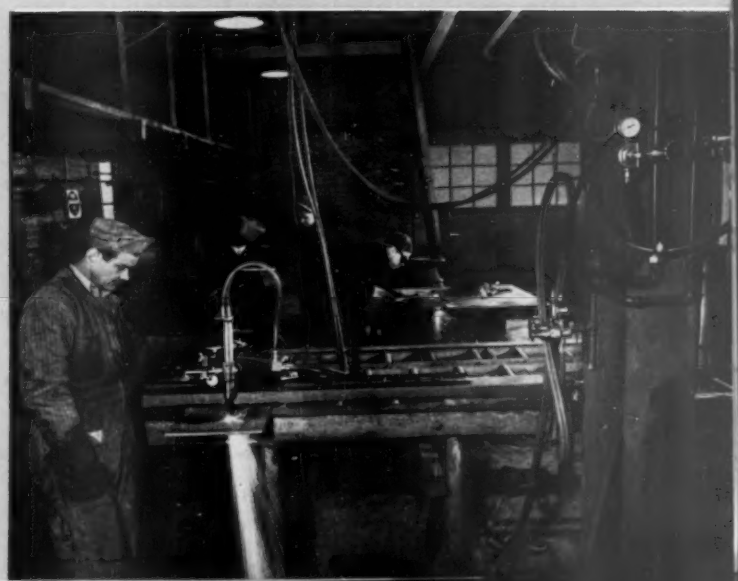
BUSINESS MEETING
BOARD OF DIRECTORS MEETING

THURSDAY EVENING, OCTOBER 18th — 7:30 P.M

Grand Ball Room

ANNUAL DINNER
Presentation of Awards

Above: Efficient, low-cost cutting of scrap is assured at the Bronx Iron and Metals Corp., New York, with use of Torchweld 75 Hand Cutting Torches and an up-to-date pipeline gas supply system. Piped from a trailer-serviced oxygen storage bank and an acetylene manifold, gases are conveniently at hand all over the yard from well-type stations, like the one seen in the picture. The steel well-cover closes when the station is not in use to permit normal traffic and yard use of the ground area.



At right: "We have reduced our gas consumption by 33⅓% and substantially cut our labor costs since standardizing on Torchweld equipment and installing an NCG manifold system," says Harvey Wallert, shop foreman; and Gene De Buck, maintenance superintendent, of Knoebel Iron Works, Inc., Chicago fabricators specializing in building steel. "The Torchweld No. 36 cutting torches we use really stand up in hard use. For instance, we use templates for hole piercing ¼-inch stock, and that puts a torch to a tough test. We also use NCG automatic flame cutting equipment, and Sureweld electrodes for electric arc welding."

Short-cuts to Lower Costs

NCG know-how boosts efficiency, increases output and lowers costs in plants of every kind and size throughout the country

Components for barges, towboats, derrick boats and other types of vessels are speedily cut from plate in the shops of the Hillman Barge and Construction Co.'s plant at Alicia, Pa. These two NCG RS Cutting Machines work directly from patterns made in the Hillman pattern loft to cut required shapes. Hillman, designers as well as fabricators, have used NCG products exclusively for the past 10 years.

One of the big answers to today's problem of holding down production costs is the use of more efficient tools . . . tools that will get the job done better and with less working time. If you *weld metals* with the electric arc . . . if you *weld them*, *braze them*, *cut them* or *treat them* with the oxy-acetylene flame . . . you'll find that NCG has the tool that will do it with top efficiency. Whether it's on a torch, a welding machine, an electrode or a cutting machine, you'll find—as so many others have—that the NCG trademark means you'll get the job done faster, with less effort, and with better results. And, *especially important now*, you'll find that NCG equipment lasts longer and requires less maintenance.

Representatives at 51 NCG offices and authorized NCG dealers from coast to coast can show you why it pays you to buy NCG products. Call the one nearest you now, or write us.

NCG®

EVERYTHING FOR WELDING

RELY ON NCG

NATIONAL CYLINDER GAS COMPANY

Executive Offices:

858 N. Michigan Avenue, Chicago 11, Illinois

Copr. 1951, National Cylinder Gas Co.

FRIDAY MORNING, OCTOBER 19th — 9:30 A. M.
Two Simultaneous Sessions

21—INERT-ARC WELDING

Crystal Ball Room

Chairman—O. H. KUHLKE
General American Transportation Co.

Co-Chairman—H. N. SIMMS
Black, Sivalls & Bryson, Inc.

Aircomatic Welding-Refinery Components and Pressure Vessels
by S. YACZKO, United Engineers & Constructors, Inc.

Sigma Welding of Carbon Steels
by H. T. HERBST, and T. Mc ELRATH, JR., Linde Air Products Co.

Performance of High-Strength Aluminum Alloy Weldments
by W. R. APBLETT and W. S. PELLINI, Metallurgy Div., Naval Research Lab.

Thoriated Tungsten Electrodes—Their Welding, Characteristics and Applications
by G. J. GIBSON and R. O. SEITZ, Air Reduction Sales Co.

22—METALLIZING

Italian Garden

Chairman—W. B. MEYER
Nooter Corp.

Co-Chairman—S. A. GREENBERG
Secretary A.W.S. Committee on Metallizing

The Metallizing Process
by F. J. KELLER, Aluminum Research Labs.

Modern Developments in Metallizing
by SAM TOUR, Sam Tour and Co.

Today's Metallizing Applications
by K. B. SMITH, Dix Engineering Co.

Exhibition of Specimens and Applications

NEW LITERATURE

Cutting and Welding Fires

The N.F.P.A. has reissued its valuable illustrated pamphlet, "Preventing Cutting and Welding Fires," No. 51-A. Changes have been made to bring this pamphlet up to date and to make it consistent with current recommendations of the N.F.P.A. Committee on Gases. Single copies may be obtained free on request and additional copies at 15 cents each from the National Fire Protection Assn., 60 Batterymarch St., Boston 10, Mass.

Hobart Arc Welding News

Volume VIII, No. 1, "Hobart Arc Welding News," a 16-page booklet of interesting photographs and articles on welding from all over the country, is now available. Copies are mailed free of charge to anyone interested in arc welding.

Many of the articles feature time-and-money saving applications. To get your copy, write the Hobart Brothers Co., Troy, Ohio.

Spot Welding

A convenient reference chart has been published by Ampco Metal, Inc., giving the recommended schedules for spot welding low-carbon steel.

This chart is a standard size ($8^1/_2$ x 11 in.) and is punched to fit any three-ring binder.

Copies can be obtained gratis by writing Ampco Metal, Inc., 1745 S. 38th St., Milwaukee 46, Wis.

Hard-Facing Alloys

The Alloy Rods Co., York, Pa., has just issued a de luxe 16-page bulletin on the new line of hard-facing alloy electrodes. Technical information on each type is given with typical applications. Technical tables are given for various types of applications with recommendations for the electrode to be used.

Flame-Hardening Machine

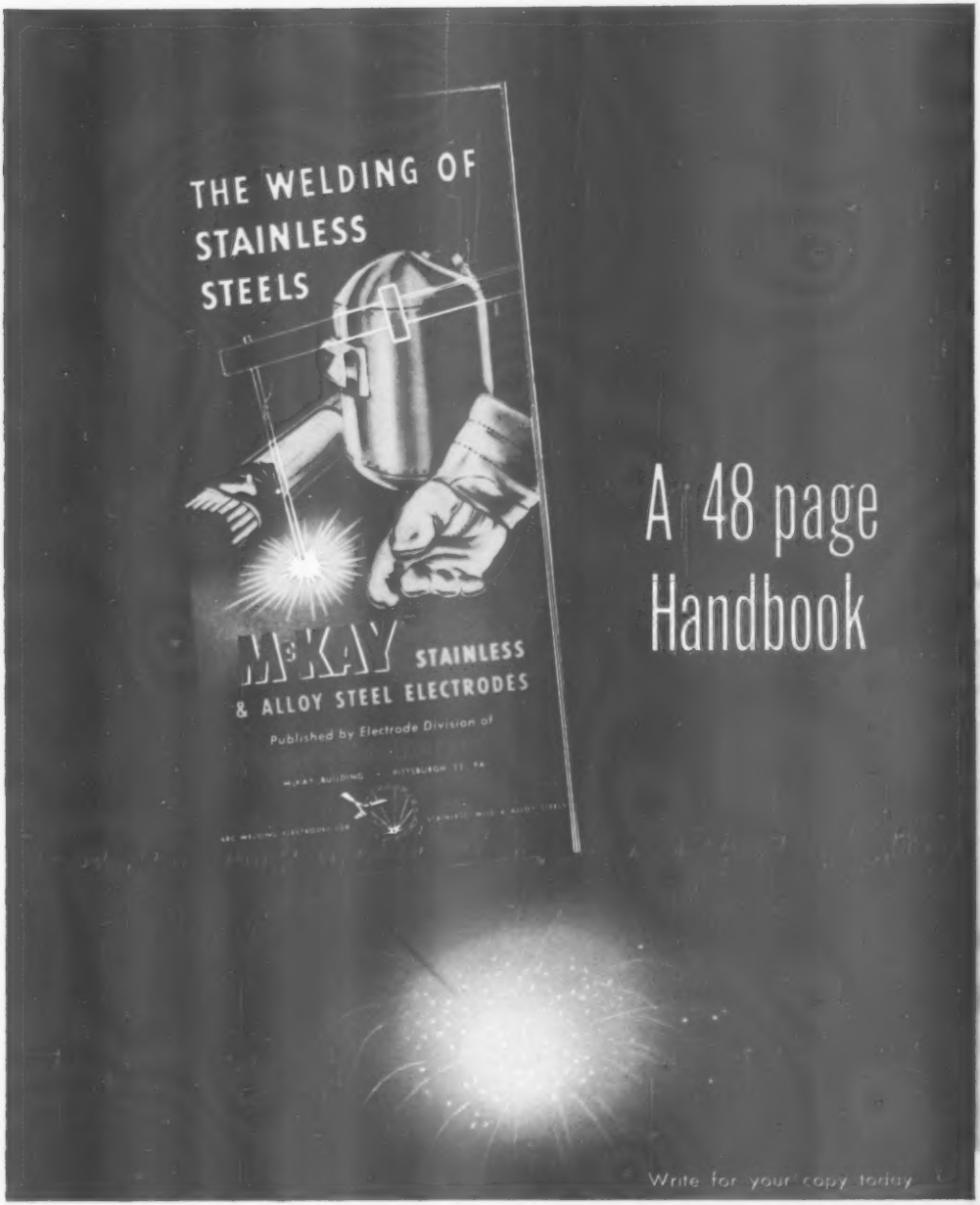
The Stearns-Roger Manufacturing Co., Denver 2, Colo., has issued a 20-page, heavy-covered technical bulletin on the Universal Flame Hardener, giving complete technical description of the flame-hardening procedure to be used in various applications. Technical information on the metallurgical side of flame hardening is given in this unusually attractive bulletin.

Brazing Data

Of especial importance to the return of women to war plants is a 4-page folder ($3^1/_4$ x $6^1/_4$ in.) now available from All-State Welding Alloys Co., Inc., 273 Ferris Ave., White Plains, N. Y. Folder gives easy-to-follow instructions on brazing sheet and cast aluminum and reflects the ease with which novices can acquire skill in fabricating even thin aluminum with the special alloys developed since World War II. Copies are available to anyone, with or without company connections, on request.

National Production Authority (NPA)

The National Production Authority, U. S. Department of Commerce, in May issued a list of more than 4000 manufactured articles containing steel, copper or aluminum which are designated as Class B products under the Controlled Materials Plan which became operative on July 1st. The Class B list shows products arranged under category headings. It also indicates the particular NPA Industry Div. to


THE WELDING OF STAINLESS STEELS
McKAY STAINLESS & ALLOY STEEL ELECTRODES
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which manufacturers should apply for allotments of controlled materials (steel, copper and aluminum). In the list, "Acetylene Welding and Cutting Apparatus" is under the NPA's General Industrial Equipment Div. and assigned Product Class Code No. 35425. "Gas Cylinders" come under the NPA's Containers and Packaging Div., with the Product Class Code No. 34434. (From *I.A.A. News Bulletin*, July 1951.)

Maintenance Supplies

The current issue of the *Metco News* describes and illustrates how you can stretch your "Maintenance and Operating Supplies" dollars under NPA Regulation No. 4. For instance, a Pennsylvania refinery metallizes hundreds of worn machine parts. Typical of these are pump rods ranging in diameter from $^3/_4$ to $3^1/_2$ in. and from 4 to 8 ft. in length. Due to severe and varied operating conditions wear occurred rapidly at the packing areas, and formerly resulted in frequent replacement of the packing as well as the rods. This was particularly true on hot and cold gasoline pumps where rod life varied from two months to as little as two weeks.

However, by metallizing each worn rod, and remetallizing as wear occurs again, they have eliminated the purchase or manufacture of replacements completely. In case a rod breaks or is otherwise damaged beyond repair, they replace it merely by turning one out of ordinary cold-rolled steel and metallizing the surface with stainless steel. Average cost of salvaging these rods is about 16% of replacement cost, and in one year this user saved 4000 lb. of alloy steel and 2200 man-hours.

Send for your copy of the *Metco News*, Volume 5, Number 9, Metallizing Engineering Co., Inc., 38-14 30th St., Long Island City 1, N. Y.

Welding Accessories

A new bulletin of arc-welding accessories is available from the Cam-Lok Division of Empire Products, Inc., Cincinnati. The folder illustrates and describes the features and advantages of cable connectors, electrode holders and terminal connections manufactured by Cam-Lok.

A copy of the bulletin may be obtained by writing to: Cam-Lok Div., Empire Products, Inc., P. O. Box 98 C-64, Cincinnati 36, Ohio.

Ampco Welding News

The second quarter issue of the *Ampco Welding News* is available upon request.

One case history reveals the techniques of welding with Ampco-Trode electrodes on a centrifugal separator where high-strength, high-thermal conductivity and maximum resistance to wear, corrosion, erosion and cavitation were the requirements.

Ampco-Facing screw conveyor shafts, cable idlers and a cement mill pinion are a few of the other case histories included in this informative issue. Ampco Metal, Inc., 1745 S. 38th St., Milwaukee 46, Wis.

Cable Plugs

The Joy Manufacturing Co. announces completion of a two-color bulletin that illustrates and describes their push-lock type plugs for portable feeder cables to welding machines. Specially designed for welding needs, these plugs are reported to be easily engaged, yet almost impossible to disconnect accidentally.

Molded as one-piece Neoprene rubber units they are available molded-to-cable lengths of customers specifications or in the newly developed attachable type. According to the manufacturer they will not crack or shatter and cannot become out-of-round.

For complete details and a copy of this new bulletin (*No. SM-2*) write Joy Manufacturing Co., Electrical Connector Div., Henry W. Oliver Bldg., Pittsburgh 22, Pa.

Tweezer-Weld Catalog Available Now

An eight-page bulletin is available on Tweezer-Weld's new Bench Head line for small parts welding. The line feature Bench Heads of great diversity, single-pair electrodes, double-pair electrodes, 2 welds simultaneously and Bench Heads with automatic wire feed and cutoff arrangements. Micrometer-like pressure adjustments provide pressures from 6 oz. to 15 lb. All embody the Tweezer-Weld automatic instantaneous follow-through feature. Ask for Bulletin No. 253. Readers please address inquiries to Federal Tool Engineering Co., 532 Mulberry St., Newark 5, N. J.

Stud Welding Operating Manual

A new 32-page, pocket-size *Stud Welding Manual* has been issued for industry use by KSM Products, Inc., Merchantville, N. J.

The Operating Manual is directed to the operator of the equipment. Featured, for the first time, is a step-by-step photographic explanation of "How to Stud Weld." The sequence includes 14 photographs of the actual stud welding process.

Other vital data, supplemented by appropriate diagrams and pictures, includes a description of stud welding equipment and accessories; directions for hooking up the equipment; and approximate generator and timer settings. The booklet concludes with sections on "How the Weld Is Made"; "The Types of Welds You Can Get"; and "Do's and Dont's of Stud Welding."

A copy of the *Stud Welding Operating Manual* may be obtained by writing to Engineering Dept., KSM Products, Inc., 6512 Park Ave., Merchantville, N. J.

1950 Supplements to Book of A.S.T.M. Standards

To keep up to date the triennially published *Book of A.S.T.M. Standards*, the American Society for Testing Materials in the intervening years issues Supplements to each part of the Book. The *1950 Supplements*, recently issued in six parts, give in their latest approved form some 353 specifications, tests and definitions which were either issued for the first time in 1950 or revised since their appearance in the 1949 Book.

Part 1 on Ferrous Metals consists of 328 pages and includes 66 standards covering steel (pipe and tubes, castings, bolting material, boiler plates, structural and rivet, bar, forgings, heat-treated tires and springs, concrete reinforcement, corrosion and heat resisting, and metallic coated steel products); cast and malleable iron; ferro-alloys; metallography; and general testing methods.

Part 2 on Non-Ferrous Metals contains 232 pages, including 42 standards covering copper and copper-base alloys (wire, rods and stranded conductors for electrical purposes, plate, sheet and strip, wire, rods, bars and shapes, pipe and tubes); aluminum and copper wire, rods and stranded conductors; aluminum and aluminum-base alloys; die-cast metals and alloys; and general testing methods.

Part 3 on Cement, Concrete, Ceramics, Thermal Insulation, Road Materials, Waterproofing Soils has 364 pages; Part 4 on Paint, Naval Stores, Wood, Adhesives, Paper, Shipping Containers, 352 pages; Part 5 on Textiles, Soap, Fuels, Petroleum, Aromatic Hydrocarbons, Antifreezes, Water, 596 pages; and Part 6 on Electrical Insulation, Plastics, Rubber, 296 pages.

The *1950 Supplements*, in heavy paper cover, can be obtained from the American Society of Testing Materials, 1916 Race St., Philadelphia 3, Pa., at $3.50 per part—or $21 for the complete set of six parts.

Shipyard Stud Welding Applications Described

A 26-page application portfolio detailing the current range of applications of Nelson stud welding in ship construction is available on request to the Nelson Stud Welding Div., Gregory Industries, Inc., Lorain, Ohio.

Diagrams and descriptive text illustrate various application procedures by which the split-second fastening method is being used to cut costs in installing wood decking; insulation materials; electrical conduit, wireways and pipe hangers; airports and window frames; furniture and other fixtures; in securing grating, manhole covers, cargo batten and in other uses. Also included is descriptive matter on new Nelwelder power units, both rotating and battery-operated, and a reprint of Bureau of Ships' shop technique folder No. 98, completely describing the use of stud welding in reference to naval construction and maintenance.

if you do D.O. WORK
if you use ALLOY ARC WELDING ELECTRODES
this book* belongs on your desk
*Written by Richard K. Lee, vice-president in charge of Research and Engineering, and George M. Hohmann, manager of Defense Production Division.
WELDING TECHNIQUES have changed markedly since World War II. This concise, authoritative, and timely brochure reviews past experience . . . describes new welding methods used in current Defense Production . . . and discusses new electrodes: their properties and uses.
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NEW PRODUCTS

New Electrode for Chrome-Moly Steel

The constantly increasing emphasis on the use of chrome-moly steel not only for aircraft and ordnance, but in a host of other repair and production applications in industry stimulated the research leading to the development of a new "EutecTrode 70" announced recently by R. D. Wasserman, President, of Eutectic Welding Alloys Corp., Flushing, N. Y.

This new "EutecTrode 70," it was reported, is a chrome-and-nickel content electrode that is specifically designed for repair and maintenance work. The special composition of this welding alloy, coupled with Eutectic's unique "Frigid-Arc" coating, is claimed to result in great ease of application in all positions and an extremely smooth arc action. Other advantages are said to include high tensile strength plus an unusually low amperage application that minimizes heat defects in the weld area.

EutecTrode 70 is manufactured in the following sizes: 1/16, 5/64, 3/32, 1/8, 5/32 and 3/16 in. diameters. Complete specifications and data may be obtained by writing Dept. P, Eutectic Welding Alloys Corp., 172nd St. & Northern Blvd., Flushing, N. Y.

New Arc Drive Control for Welders Rectifier

A new arc drive control to produce electrical transient characteristics desirable for certain welding operations now is available on Type RA selenium rectifier welders, manufactured by Westinghouse Electric Corp.

Arc drive permits the operator to adjust the arc characteristic of the welder to suit a particular job. The device operates automatically and instantaneously to provide an extra surge of welding current at the moment the arc becomes shorted, either by contact between the electrode and the work or by a globule of molten metal bridging the arc gap. This extra surge of current helps clear the arc path and prevents arc outage or sticking of the electrode. Violent and prolonged overshoots and undershoots of welding current, characteristic of rotating types of welding generators, are eliminated.

The amount of arc drive desired can be set by the operator. Adjustment of the control is not critical. It has an "off" position, for use when the machine is to be used short-circuited on low-resistance loads for any appreciable length of time—such as resistance heating.

A self-heating thermostat is provided in the arc drive circuit to protect it from overheating due to sustained short-circuit of the welding terminals.

For further information, write Westinghouse Electric Corp., Box 2099, Pittsburgh 30, Pa.

Mechanized Heliarc Welding of Steel Elevator Doors

The Otis Elevator Co. Ltd., Hamilton, Ontario, have saved an estimated 3000 man-hours per year by the adoption of mechanized Heliarc welding for welding the edges of steel elevator doors. A synopsis of the operation follows:

The doors are located in an air-operated fixture that eliminates manual handling. The welding machine proper consists of a Heliarc HW-4 torch mounted on an Oxweld CM-21 machine carriage which travels on a two-rail track.

Fig. 1 An elevator door is being welded in this jig with a HELIARC HW-4 torch at a speed of 22 in. per minute

The welding zone is shielded by a rectangular box fitted with a colored-glass window. The box cuts down consumption of shielding argon gas, and the colored glass window eliminates the need for a welding helmet or goggles being worn by the operator

Welds are made with a 1/8-in. tungsten electrode traveling at a speed of approximately 25-in. per minute, and using a current of 135 to 140 amp. A high-frequency unit is used for starting the arc; therefore, the electrode can be kept the same distance from the work during the whole operation. The operation can be observed at all times through a vision panel located in the box that shields the welding zone.

By the adoption of the Heliarc principle and the elimination of manual welding and grinding on hollow metal doors, the Otis Elevator Co. Ltd. have improved the quality of the doors produced and appreciably decreased the manufacturing time.

Eliminating Glare

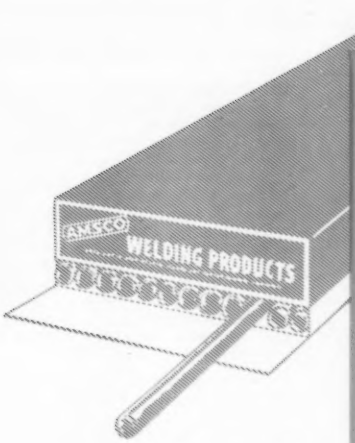
Silvo-Glo Lens, manufactured by Steinwach's Safety Supply, 369 Ellicott St., at Huron, Buffalo 3, N. Y., sharpens outline of objects being welded. It is thin, mirror-like reflective in appearance and improves the quality of work through better vision. It reduces eye strain by reflecting the rays and the heat given off by the work. It does away with the use of an expensive coated filtering lens.

Air Grinder

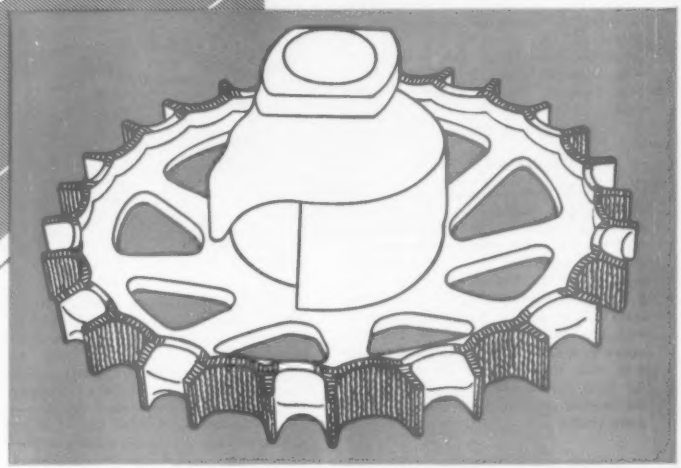
A new air grinder, just announced by Ingersoll-Rand, has an added safety device to prevent overspeed operation and possible accidents. It has special application in foundries, steel mills, general manufacturing and metalworking plants where hand grinding is required for snagging, trimming, smoothing, etc.

The new grinder contains a motor governor to maintain correct wheel speed, plus a built-in unit called the "overspeed safety coupling." In case the motor overspeeds because of governor wear, abuse, maladjustment or dirty air, the "overspeed safety coupling" automatically uncouples the arbor and the grinding wheel from the motor, making it impossible to operate the grinder until the cause of overspeed has been corrected.

Another safety feature of the grinder is a multiple exhaust system. The



How AMSCOATING with Amsco Hardfacing Rods saves you materials . . . manpower . . . money.



These teeth take bites out of repair costs!

AMSCOATING can make old Tractor Drive Sprockets last 3 times longer than new.

AMSCOATING...stands for control of wear by Hardfacing...

Hardfacing rods—and recommendations for their use—are as sound as the manufacturer who makes them. AMSCO has been fighting wear for a half-century—first with Manganese Steel, and later with AMSCO Hardfacing Products.

If you have a problem of wear caused by impact, abrasion, heat or corrosion . . .

Find out how AMSCOATING can save you materials . . . manpower . . . money!

Here's another way to beat the high cost of replacements, down-time and maintenance . . . AMSCOATING the teeth on Tractor Drive Sprockets. Time after time service records have shown that AMSCOATING of sprockets results in these dollar-saving advantages:

1. ***On an average — 3 times the service life of a new, unhardfaced sprocket.***
2. ***2 out of 3 sprocket changes completely eliminated . . . far less down-time, less maintenance . . . more equipment out of the shop and on the job.***

To top it off, sprocket replacements can practically be eliminated. An AMSCOATED sprocket can be reclaimed over and over again . . . *at about half the cost of a new one!*

Get all the facts on how to make these big savings . . . write today for instruction sheet on AMSCOATING sprockets, and the name of your nearest AMSCO Distributor.

AMSCOATING

THE RIGHT WAY TO SAY HARDFACING

AMERICAN Brake Shoe COMPANY

AMERICAN MANGANESE STEEL DIVISION

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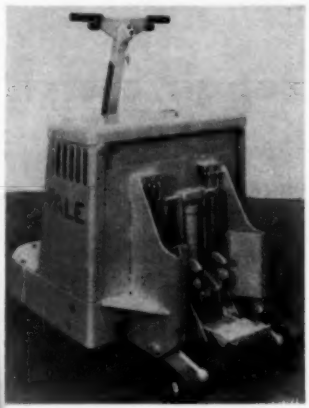
Other Plants: New Castle, Del., Denver, Oakland, Cal., Los Angeles, St. Louis. In Canada: Joliette Steel Division, Joliette, Que.
Amsco Welding Products distributed in Canada by Canadian Liquid Air Co., Ltd.

operator can choose any one of four exhaust positions, spaced 90 degrees apart, and thus direct the exhaust away from the work and from his own body, if he desires.

Further information can be obtained from Ingersoll-Rand Co., 11 Broadway, New York 4, N. Y. Ask for *Bulletin 5096*.

Motorized Tractor

A new welded motorized tractor that automatically engages various types of industrial trailers will permit operation in very narrow aisles and accurate spotting of loaded trailers it was announced here recently by the Philadelphia Div., Yale & Towne Manufacturing Co. In actual operation on a large trailer it is possible with this device to engage the end of a caster type trailer and make a right angle turn with the center of the turn being the fixed axle of the trailer. This gives the shortest steering radius possible with a given trailer.



For further information, contact the Philadelphia Div., Yale & Towne Manufacturing Co., 11,000 Roosevelt Blvd., Philadelphia 15, Pa.

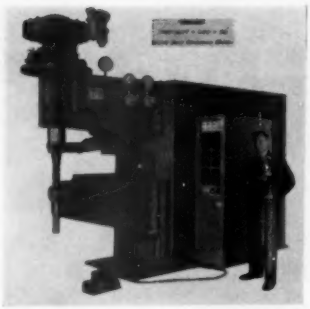
Spot Welder

The most powerful spot welder ever built might be an appropriate designation for this "Three-Phase, Modu-Wave" machine field of electric resistance welding for Sciaky Bros., Inc., Chicago, Ill.

With an electrode force adjustable up to 23,000 lb. and a rating of only 400 kva., this machine is capable not only of meeting, but actually exceeding, the rigid requirements of the Air Force-Navy Aeronautical Specifications AN-W-30 (MIL-6860) and AN-W-32 (MIL-6858).

Up to two thicknesses of $^1/_4$-in. aluminum alloys can now be spot welded with consistently high-quality results on a heavy-production basis.

To achieve this dependability and consistency of performance, certain control functions and other features were brought into play as follows: (1) An extremely



rapid rate of rise of the forging pressure was achieved through the use of a frictionless diaphragm pneumatic pressure system. Without the leathers that rub against the walls of conventional cylinder head machines, friction was eliminated and quick follow-up pressure was assured. (2) A "tailor-made" wave shape, adjustable to suit any given spot welding application, was obtained by virtue of the Sciaky Three-Phase, Modu-Wave System—thus eliminating the necessity of making prolonged compromises between current, timing and pressure sequence as would be the case with conventional equipment. (3) The current conductance efficiency of the secondary circuit was vastly improved by the adaptation of solid electrolytic copper bars with sliding silver contacts at each terminal for transmitting the secondary current from the transformer to the movable electrode.

Power Attachment for Positioners

Aronson Machine Co., Arcade, N. Y., has recently redesigned their Universal Power Attachment which is an optional extra on their line of Universal Balanced Positioners. (See photo below.)

The characteristic features of the Power Unit are:

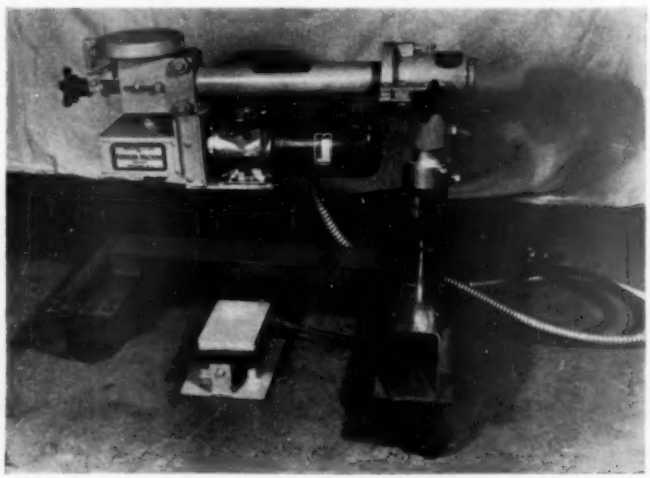
- Infinitely variable speeds from 0 to 6 rpm.
- Quick engage and disengage of the power transmission for use of power rotation or manual rotation and positioning.
- Worm and worm-gear final drive adjustable to virtually eliminate backlash on the work table.
- Friction clutch coupling to allow operator to position workpiece on the rotational axis whether table is turning or not. This would be necessary in the event the operator was tacking a job that required he quickly get a 0, 90, 180 and 270 degree position to prevent warpage just prior to making a continuous weld on a circumferential object.

One other big feature of this attachment is that it can be installed so easily. The customer would merely loosen and take off the original work-table spindle-block assembly and slip on the entire new unit. This can be done in about 5 to 10 min.

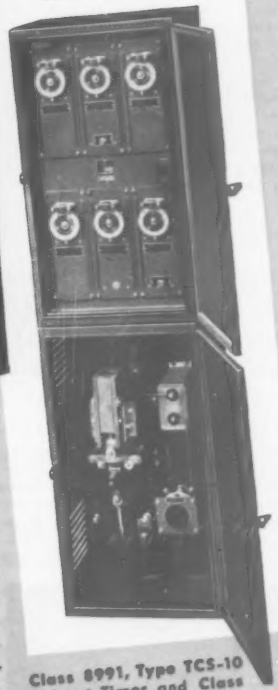
Standard power characteristics is 110/220 v., single phase, 60 cycle. For other power contact the factory.

Remote Voltage Control for Arc Welders

The Mullenbach ARCTROL is a foot-operated variable resistor that plugs into many standard welding units and can be quickly installed on most others. It gives instant selection or variation of any welding heat within capacity of the machine wherever the operator is working. Because of its flexibility of control, the Arctrol is extremely useful on precision welding operations. Voltage can be varied during welding, instantly changed to meet variations in fit-up and position, or even to extinguish the arc at completion of the weld, this avoiding cratering. Faster welding speed results because heat can be



Specify.. SQUARE D Safront RESISTANCE WELDER CONTROL



FOR CONTINUOUS PRODUCTION—heavy duty rugged construction, hardened moving parts, and accurate, dependable pneumatic timer units.

FOR OPERATOR SAFETY—Safront panel construction isolates individual timer adjustments from live parts. Transformer provides 110 volts for all control circuits.

FOR MOUNTING FLEXIBILITY—Dimensions are standardized. Timers and contactors can be mounted in four different positions for wall or machine mounting.

FOR LOW INSTALLATION COST—Matching knockouts permit close coupling of enclosures, eliminating need for conduit run between contactor and timer.

FOR SIMPLIFIED MAINTENANCE—Accessible mechanical parts and easy coil replacement reduce "down" time and maintenance costs.

When extreme precision or speed is required—use Square D's Electronic Welder Control. Write for Bulletins 8990-8993 which detail the complete Square D line, magnetic and electronic. Address Square D Company, 4041 N. Richards Street, Milwaukee 12, Wisconsin.

Class 8991, Type TCS-10 Safront Timer and Class 8990, Type SCG-2 Syncro-Break Contactor arranged for wall mounting. The same size, neat appearing enclosure is used for 4 and 6 period Safront timers and sizes 1W and 2W Syncro-Break contactors.

SQUARE D COMPANY

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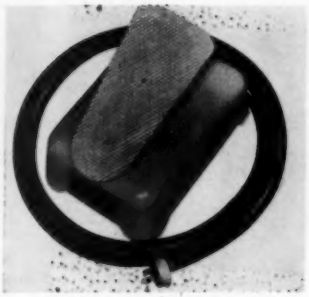


kept at the most efficient peak for the job conditions. Arctrol improves soundness of welds by providing a hot start, which helps overcome porosity and cracks on cold work. After 2 or 3 in. have been welded, voltage is easily tapered off, balancing heat input with dissipation.

The Mullenbach Arctrol can be applied to most d.-c. welders, and to some types of a.-c. machines. Its use in no way limits the capacity of the welder nor use of the regular voltage control. If desired, the latter can be set at any predetermined position below maximum. The Arctrol will then give a complete range of control up to this preset voltage.

Prices and information on application of the Arctrol to specific welding machines is available from the manufacturer: Mullenbach Electrical Manufacturing Co., 2300 E. 27th St., Los Angeles 58, Calif.

Employment Service Bulletin

Position Vacant

V-256. Welding Engineer. Plant in southeastern Pennsylvania doing heavy fabrication of machinery and component parts. Minimum five years' experience or B.S. degree, Met., M.E., or Chem. Eng., plus general knowledge of welding techniques and procedures. Contacts with customers and plant technical personnel. Age 30–40. State salary desired first letter.

V-257. Welding Engineer. Unusually fine opportunity for engineer with M.E. degree or equivalent, having alloy steel welding experience and supervisory potential. This is a new project and offers the right man excellent possibilities for growth.

V-258. Junior Welding Engineer. Excellent opening for recent graduate welding or metallurgical engineer with some practical experience in welding. Position involves welding development, procedures and quality control. Large eastern fabricator of carbon steel and alloy materials. Please furnish details of experience, education, age, and expected salary.

Welding Engineer. General Electric has opening for a Welding Engineer. With engineering degree to work on jet propulsion engines at Lockland, Ohio (near Cincinnati), plant. Send résumé to Technical and Supervisory Personnel, Aircraft Gas Turbine Div., General Electric Co., 920 Western Ave., West Lynn 3, Mass.

Services Available

A-619. Position wanted as welding engineer. College graduate with eleven years' engineering experience, past four as welding engineer with large sheet metal fabricator. Excellent background in resistance and fusion welding, induction heating, brazing and soft soldering. Married. Age, 32.

PERSONNEL

Harry Pierce Promoted

The election of Harry W. Pierce as Vice-President of the New York Shipbuilding Corp., Camden, N. J., has been announced by T. H. Bossert, President of the Corporation. Mr. Pierce will be in charge of design and procurement activities.

Mr. Pierce is a graduate of the U. S. Naval Academy, Class of 1922, and received his master's degree in Naval Architecture from Massachusetts Institute of Technology in 1926.

Mr. Pierce joined the New York Shipbuilding Corp. in 1930 after serving as Naval Constructor, U. S. Navy. Until 1947, most of his work with the corporation was connected with welding in Ship Construction. In 1947, Mr. Pierce was named Assistant to the President and in that capacity has had charge of administrative and technical functions.

During World War II, Mr. Pierce handled several projects for the Bureau of Ships, Navy Dept., and received the Certificate of Commendation from the Bureau. He also administered the landing craft construction program for New York Shipbuilding Corp. during World War II. He has served on many advisory committees dealing with the investigation of welded stresses in ships, steel for ship construction and corollary investigations. In 1947, he was granted a four-month leave of absence to assist in a study of the Japanese shipbuilding industry for the War Dept.

Mr. Pierce is President of the AMERICAN WELDING SOCIETY, a Past-President of the Maritime Society of Philadelphia, a former Vice-President and a Director of the Engineers Club of Philadelphia and a member of the Society of Naval Architects and Marine Engineers, the American Society of Naval Engineers, and the American Society for Testing Materials. For the past two years he has also been a consultant to the Office of Production Planning, Munitions Board.

Palmer Made Regional Manager

Walter E. Palmer of Cresskill, N. J., has been appointed Regional Manager for All-State Welding Alloys Co., Inc., to cover New Jersey, Eastern Pennsylvania, Maryland, Delaware and the District of Columbia. Mr. Palmer will be responsible for sales and service to the users and distributors of AllState Alloys and Fluxes in his area.

With more than twenty-six years of experience in selling and servicing special alloys, Mr. Palmer is particularly well equipped for his new responsibilities. He is an old-timer in the AMERICAN WELDING SOCIETY and has done a vast amount of experimental work in the use of chemical, metallurgical and mechanical arts for the improvement of welding materials and practices.

Previous to joining the All-State organization, Mr. Palmer was Eastern Manager for Hollup Corp., Field Sales Manager for Alloy Rods Co. and Special Representative for the McKay Co. Throughout his career he has worked with and through distributors and has made a host of friends in the welding supply business.

Mr. Palmer studied engineering at New York University. He is married and has three children. His hobbies are miniature car racing, car models, engines and chemical and welding experimenting.

OBITUARY

W. W. Reddie

W. W. Reddie, long-time member of the A.W.S. Manufacturers Committee and long active in the A.W.S. national activities as well as the Pittsburgh and Buffalo Sections' activities, was killed in a boat accident on the Niagara River at Buffalo on Friday, July 27th.

Funeral services were held Tuesday, July 31st, in the city of his family, Brazil, Ind.

Mr. Reddie was born on Dec. 14, 1889, in Brazil, Ind. Graduated from Rose Polytechnic Institute with a B.S.E.E. degree, he was employed on the Student Course of Westinghouse Electric Corp., at East Pittsburgh, Pa., on July 8, 1912. Mr. Reddie worked through various positions in the Arc Welding Sales Dept., among them being Section Manager and Assistant to Manager.

In September 1946, he was transferred to Buffalo, N. Y., along with the Welding Dept.'s move from Trafford, Pa. At the time of his death he was in charge of Market Planning and Cost Analysis in the Arc Welding Dept.

F. J. Ryder

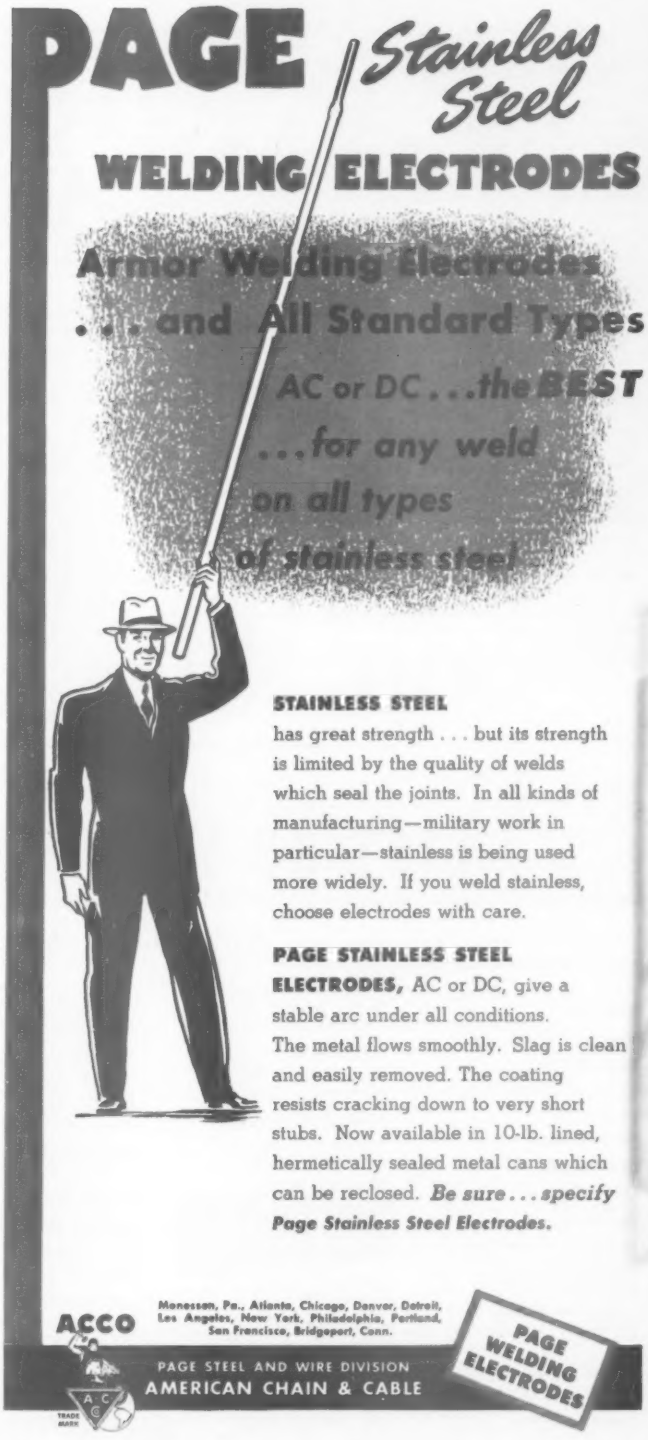
F. J. Ryder, M.E.I.C. Superintendent of Plant No. 1 for The Canadian Bridge Co. Ltd. in Walkerville, Ontario, died suddenly on June 22nd at Windsor, Ontario.

Born at Holyoke, Mass., in 1907, he graduated from McGill University in Civil Engineering in 1929. After graduation he was employed in the Drafting Dept. of The Canadian Bridge Co. Ltd. in Walkerville. In 1938, he accepted the position of Sales Engineer, with the same company in their Toronto Office and served in that capacity until 1943, when he was transferred back to the head office in Walkerville to assist in the planning and production of war materials. In 1945 he was appointed to the position of Assistant Shop Superintendent of Plant No. 1 in Walkerville, becoming Superintendent, in 1949.

Mr. Ryder had many interests and activities and was a member of the McGill Alumni, All Saint's Anglican Church, the Association of Professional Engineers of Ontario, The Engineering Institute of Canada and the AMERICAN WELDING SOCIETY.

He joined the Engineering Institute of Canada as a Student in 1928, transferring to Junior in 1935 and to Member in 1946. He was closely associated with the activities of the Border Cities Branch and had served as Secretary-Treasurer, Chairman and Councillor and at the time of his death was serving as branch member of the Nominating Committee.

He is survived by his wife and two sons, residing at 1960 Arras St., Windsor, Ontario.


PAGE
Stainless Steel
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Armor Welding Electrodes
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AC or DC ... the BEST
... for any weld
on all types
of stainless steel
STAINLESS STEEL
has great strength ... but its strength is limited by the quality of welds which seal the joints. In all kinds of manufacturing—military work in particular—stainless is being used more widely. If you weld stainless, choose electrodes with care.
PAGE STAINLESS STEEL ELECTRODES, AC or DC, give a stable arc under all conditions. The metal flows smoothly. Slag is clean and easily removed. The coating resists cracking down to very short stubs. Now available in 10-lb. lined, hermetically sealed metal cans which can be reclosed. Be sure ... specify Page Stainless Steel Electrodes.
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PAGE STEEL AND WIRE DIVISION
AMERICAN CHAIN & CABLE
PAGE WELDING ELECTRODES

John Lewis Luckenbach

We announce with great sorrow the death on July 4, 1951, at the age of 67, of John Lewis Luckenbach, Chairman of the Board of Managers of the American Bureau of Shipping.

He had been associated with the American Bureau of Shipping for the past twenty-four years, joining the organization in 1927 as Executive Vice-President. Following the death of Capt. Charles A. McAllister in 1932, he became Acting President and was elected President in 1933. He continued in this office until January 1950, when he was elected Chairman of the Board of Managers.

During Mr. Luckenbach's long period of association with the Bureau he was tireless in its interests, sponsoring many ideas for the advancement of ship classification work and improvements in the science of shipbuilding and design. He was instrumental in providing for the Bureau its home office in New York City. Entirely because of his planning, the office building at 45 Broad St. was rehabilitated as the main world headquarters for the Bureau, with full laboratory facilities, technical departments and modern office space.

Mr. Luckenbach was born at Kingston, N. Y., on Nov. 19, 1883, the son of Edward and Henrietta (Weber) Luckenbach. He graduated cum laude from Holbrooks Military Academy in 1902 and was in the class of 1906 at Princeton University. He also studied engineering in night courses at Pratt Institute, Brooklyn, N. Y. He married Kate Isobel McGregor of San Francisco on Jan. 26, 1916.

After leaving Princeton, he joined the Luckenbach Steamship Co. in New York. From 1912 to 1915 he was in charge of maintenance and repair of Luckenbach ships on the Pacific Coast. He returned to New York during 1915 to take over the design and construction of new cargo ships for the Luckenbach Line. Then followed an interlude during which he partcipated in the Government's shipbuilding program of World War I. In this activity he was assigned by the United States Shipping Board to take charge for the Government of the construction of some 35 large cargo ships in Japan and four in China. He returned in 1920 and was elected Vice-President in charge of maintenance, repair and operation of the big fleet of cargo vessels operated in the intercoastal trade by the Luckenbach Steamship Co. He served in this capacity until 1925, when he retired from the Company.

One of the original members of the Propeller Club of the United States, upon its formation in 1927, he had served on the Board of Governors for many years. Since 1936, he had been Chairman of the annual American Merchant Marine Conference sponsored by the Club.

A member of the Society of Naval Architects and Marine Engineers since 1916, he had been active in the conduct of its affairs, submitting papers at its annual meetings, serving in various offices, including the Council, and at the time of his death was an Honorary Vice-President.

Mr. Luckenbach was elected President in 1947 of the Board of Trustees of Webb Institute of Naval Architecture, Glen Cove, Long Island, N. Y. He was largely instrumental in the sale of the old school buildings in the Bronx, N. Y., and in securing the current beautiful country estate upon which the school is now located. The school and his part in it were featured in a 1950 issue of the *Saturday Evening Post*.

During the two International Safety of Life at Sea Conferences held in London in 1937 and 1948, Mr. Luckenbach served as an official delegate of the United States Government.

He was a delegate to the International Conference of Naval Architects and Marine Engineers held in London and the Engineering Congress at Glasgow in 1938.

Mr. Luckenbach represented the American Bureau of Shipping at the First Congress of Classification Societies held in Rome, Italy, in 1939.

He was a member of the American Society of Naval Engineers; The Institution of Naval Architects, London; the North East Coast Institution of Engineers and Shipbuilders, Newcastle upon Tyne; and a Vice-President of the Institute of Marine Engineers, London.

His clubs included the New York Yacht Club, India House, the Princeton Club.

LIST OF NEW MEMBERS

July 1 to July 31, 1951

BOSTON

Mendelsohn, Max (C)

BRIDGEPORT

Cleveland, George W. (C)

CHICAGO

Satek, Frank J. (C)

CLEVELAND

Gilbert, Leo (B)

DETROIT

Brendle, Russell W. (B)
Dawn, George L. (B)
Goodman, Samuel (C)
Greene, Jerry (B)
Koske, Jack R. (B)
Walsh, James V. (C)
Wood, Lorne F. (C)

HOUSTON

Hoover, J. F. (B)

LOS ANGELES

Shaffer, Harry A. (B)
Uhl, Vincent (C)

LOUISVILLE

Brown, Charles (B)
Wade, Lee S. (B)

MAHONING VALLEY

Bianco, M. J. (B)
Sherman, Alex (C)

NEW JERSEY

Greif, Herman J. (B)
Rossell, Joseph F. (C)

NEW YORK

Green, Ove (B)
Roehll, Fred F. (B)

NORTHERN NEW YORK

Nelson, Henning V. (C)

NORTHWESTERN PA.

Erhart, Dennis E. (C)

OKLAHOMA CITY

Clapp, Aaron W. (C)
House, Denver R. (C)

PEORIA

Balestri, Jerry (C)
Passent, Lester E. (C)
Ripper, Lloyd M. (C)
Stien, Joe C. (C)

PHILADELPHIA

Cressman, Franklin C. (B)

ROCHESTER

Radford, Harvey T. (B)

ST. LOUIS

Wilder, H. D. (B)

SALT LAKE CITY

Rasmussen, Don H. (B)

SYRACUSE

Fetterly, Robert P. (C)

TOLEDO

Eddins, Mike Whitley (C)

WESTERN MICHIGAN

Trierweiler, Peter (B)

WORCESTER

Welch, A. U. (B)

NOT IN SECTIONS

Finney, Joe E. (C)
Garrett, Don W. (B)
Martin, Arlie Mellon (C)
Meneses, Roberto (C)
Parks, P. G. (B)
Tezuka, Kelzo (B)

Members Reclassified

During the month of July

NIAGARA FRONTIER

Ansteth, Robert M. (D to C)
Baysinger, Frank Robert (D to B)
Brick, Arthur E. (D to C)

PUT MORE ELECTRODE HERE

LESS HERE

Good technique and the right rod for the job reduce electrode waste

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Fig. 1. *Three* electrodes used straight may give as much production as *four* which are bent before being used.

Fig. 2. *Three* electrodes burned down to a 2-inch stub may do as much as *four* when "long" stub ends are left.

Get more from your electrodes today by:

1. **Laying flat-face fillets on vertical fillet welds**
2. **Making fillet legs of equal length on horizontal fillets**
3. **Providing better "fit-ups"**
4. **Protecting welding rods through better storage and care**
5. **Avoiding bending whenever possible**
6. **Using electrodes down to a 2-inch stub**

Bending electrodes wastes about ¼ of their useful length (Fig. 1) as does the habit of throwing away "long" stub ends (Fig. 2).

NEW! Welding Electrode Slide Comparator tells in an instant the AWS designation of manufacturer's electrodes.

See your G-E Welding Distributor for your comparator. Look for General Electric under "Welding Equipment" in the yellow pages of your telephone directory. And remember, he carries a complete line of G-E welders, electrodes and accessories.

GENERAL ELECTRIC

712-7

SECTION ACTIVITIES

Prepared by C. M. O'Leary

Arizona

The regular monthly meeting of the Arizona Section was held in the Saratoga Room of the Hotel Westward Ho, Wednesday, June 20th, and attended by 30 members and guests.

Following the dinner, a very interesting talk was given by the speaker of the evening, Winfield S. McFarland of the Central Arizona Light and Power Company of Phoenix, Ariz., who spoke on the "Cause of Waste." Mr. McFarland's talk covered the prevention of waste of material, waste of time and waste of labor through proper safety practices.

The Consolidated Western Steel Corp. also presented a very interesting half-hour technicolor sound film on the making of steel, which was very informative and appealing.

The meeting adjourned at 10:00 P.M. and announcement was made of the fact that the next meeting would be held in September.

Birmingham

The following officers have been elected for 1951–52 by the Birmingham Section: *Chairman*, Alfred E. Pearson, Ingalls Shipbuilding Corp.; *1st Vice-Chairman*, D. H. Curry; *Sec.-Treas.*, E. D. McCauley, American Cast Iron Pipe Co.; *Chairman, Program Committee*, D. H. Curry; *Technical Representative*, J. E. Durstine, Lincoln Electric Co.

Boston

The following officers have been elected for 1951–52 by the Boston Section: *Chairman*, H. Hugo Stahl, Wrentham, Mass.; *1st Vice-Chairman*, P. Edward McKenna, Allston 34, Mass.; *Sec.-Treas.*, Louis I. Dexter, Pawtucket, R. I.; *Chairman Membership Committee*, Thomas B. Walsh, Avery Saul Co.; *Chairman, Program Committee*, Harry J. Ferguson, Medford, Mass.; *Technical Representative*, Harry Udin, M.I.T.

Colorado

The following officers have been elected by the Colorado Section for 1951–52: *Chairman*, Herbert B. Klodt, Denver 9, Colo.; *1st Vice-Chairman*, V. A. Schroer, Englewood, Colo.; *Sec.*, Paul C. Mathias, Denver; *Asst. Sec.-Treas.*, C. Brinton Swift, Denver; *Chairman, Membership Committee*, H. Jackson, Denver; *Chairman, Program Committee*, C. Brinton Swift; *Technical Representative*, V. A. Schroer.

Columbus

The following officers have been elected by the Columbus Section for 1951–52: *Chairman*, P. J. Rieppel, Worthington, Ohio; *1st Vice-Chairman*, H. B. Cary, Marion Power Shovel Co.; *Sec.*, M. C. Clapp, Columbus 1, Ohio; *Treas.*, R. J. Krieger, Ohio State University; *Chairman, Membership Committee*, B. G. Harrington, Jr.; *Chairman, Program Committee*, H. B. Cary; *Technical Representative*, R. J. Krieger.

Dallas

The following officers have been elected by the Dallas Section for 1951–52: *Chairman*, J. J. Brown, Grand Prairie, Tex.; *1st Vice-Chairman*, R. W. McClain, Fritz W. Glitz & Son; *2nd Vice-Chairman*, John P. Viglini, Dallas 14, Tex.; *Sec.-Treas.*, Milton E. Eliot, Mosher Steel Co.; *Asst. Sec.*, W. A. Thompson, Wyatt Metal & Boiler Works; *Chairman, Membership Committee*, A. L. Hill, Dallas Welding Supply Co.; *Chairman, Program Committee*, R. W. McClain; *Technical Representative*, W. H. Hare.

Long Beach

The following officers have been elected for 1951–52 by the Long Beach, Calif., Section: *Chairman*, Mal Teeter, Long Beach Welders Supply; *1st Vice-Chairman*, A. H. Butler; *Sec.-Treas.*, O. B. Bowers, Bowers Ambulance Service; *Chairman, Membership Committee*, Howard Epperheimer; *Chairman, Program Committee*, A. H. Butler; *Technical Representative*, Ben Laughlin.

Los Angeles

The following officers have been elected by the Los Angeles Section for 1951–52: *Chairman*, C. W. Johnston, Bell, Calif.; *1st Vice-Chairman*, H. A. Hoth, Air Reduction Pacific Co.; *Sec.-Treas.*, H. K. Clemens, San Gabriel, Calif.; *Chairman, Membership Committee*, D. P. O'Connor; *Chairman, Program Committee*, H. A. Hoth; *Technical Representative*, C. W. Johnston.

Louisville

The following officers have been elected by the Louisville Section for 1951–52: *Chairman*, A. B. Doudna, Louisville Gas & Electric Co.; *1st Vice-Chairman*, Roba Yarbro; *Sec.*, E. H. Dilley, Tube Turns, Inc.; *Treas.*, Joseph O'Regan; *Chairman, Program Committee*, Roba Yarbro; *Technical Representative*, Mason S. Noyes.

Maryland

The final meeting of the 1950–51 season was held on Friday, May 18th, at the Engineers Club of Baltimore, Md.

After-dinner entertainment was in the form of a film entitled "Dupont Nylon." The members were particularly interested in seeing the nylon material that was modeled by the young ladies.

National Technical Secretary, S. A. Greenberg, spoke briefly on code practice and then introduced the speaker of the evening, National First Vice-President, Charles H. Jennings.

Mr. Jennings' topic of the evening was "Proper Preparation for Welding." The first five steps of which are those usually taken in the Engineering Dept. and are as follows: (1) Determine the size of the product. (2) Refer to established welding standards, or good welding planning. (3) Engineer creates idea and draftsman portrays it on his drafting board, using A.W.S. symbols. (4) Investigate good design practice and metallurgical experience. (5) Reference should then be made to the Filler Metal Specifications for Arc-Welding Electrodes.

The foregoing five steps cover the work required by the Engineering Dept. The next step is to the shop. The welding operators should, of course, know and be able to follow the welding procedure. Mr. Jennings pointed out that when you know the characteristics of the electrode selected and the procedure adopted, then you can make good welders.

Mr. Jennings has been repeatedly told that it would be wonderful if a standard procedure could be prepared. Mr. Jennings, who has written hundreds of specifications, gave a specific example of the complexities that can develop. One of the reasons welding has progressed to the extent it has is because of establishing and adhering to procedures. The American Welding Society has set up a committee headed by Roger W. Clark of the General Electric Co. to simplify procedures. It welcomes suggestions.

The type of welding equipment to be used was the next subject.

Another decision to be made concerns the method of welding.

Mr. Jennings spoke from 8:00 P.M. to 9:45 P.M. with the last 15 minutes devoted to questions and answers.

The following officers have been elected for 1951–52: *Chairman*, Fritz Albrecht, Baltimore 14, Md.; *1st Vice-Chairman*, Aldridge L. Lees, Westport Station;

Sec., H. S. Sayre, U. S. Naval Eng. Exp. Station; *Treas.*, Lytle H. Matthews, Delta Oxygen Co.; *Chairman, Membership Committee*, J. Gulley; *Chairman, Program Committee*, Aldridge L. Lees; *Technical Representative*, R. E. Metius.

Milwaukee

The following officers have been elected by the Milwaukee Section for 1951–52: *Chairman*, R. P. Walbridge; *1st Vice-Chairman*, F. E. Garriott; *Sec.-Treas.*, F. J. Harencki; *Chairman, Membership Committee*, W. J. Gallo; *Chairman, Program Committee*, F. E. Garriott.

Pascagoula

The Annual Business meeting and installation of officers of the Pascagoula Section was held on July 25th at the Pascagoula Country Club.

J. F. Bryan, Jr., of the American Bureau of Shipping, gave an excellent talk on the Section prospects.

The newly elected officers for 1951–52 are as follows: *Chairman*, A. W. Mansfield; *1st Vice-Chairman*, P. L. Ware; *Sec.-Treas.*, I. W. Whittemore; *Chairman, Membership Committee*, J. S. Howkins; *Chairman, Program Committee*, J. A. Furr, Jr.; *Technical Representative*, T. J. Dawson.

Portland

The ninth meeting of the 1950–51 season was held at the Mallory Hotel, Tuesday, June 20th.

After the dinner hour, the Annual Election of Officers was held with the following results: *Chairman*, Ed Weil, Lincoln Electric Co.; *Vice-Chairman*, R. L. Howerton; *Sec.*, Stan Richardson, Air Reduction Pacific Co.; *Treas.*, A. A. Robley, Oregon State College; *Directors*, Joe ——, Rex Powers, Roy Dickeson.

After the election of officers, a very interesting presentation was made by J. B. Ross of Handy & Harman, Los Angeles, Calif. Mr. Ross spoke on the subject "Silver Brazing on the Production Line." A sound, colored movie was then shown entitled "Production Silver Brazing."

Saginaw Valley

Newly elected officers of the Saginaw Valley Section for 1951–52 are as follows: *Chairman*, J. E. Neumann; *Vice-Chairman*, D. L. Knight; *Sec.*, M. W. Blackwood, Baker Perkins, Inc.; *Treas.*, W. E. Moehring, Flint 4, Mich.

St. Louis

The St. Louis Section announces the election of officers for 1951–52 as follows: *Chairman*, Pete W. Salsich, Linde Air Products Co.; *Vice-Chairman*, Melville W. Lischer, Nooter Corp.; *Sec.-Treas.*, John J. Fox; *Chairman, Membership Committee*, Robert E. O'Niell; *Chairman, Program Committee*, Melville W. Lischer; *Technical Representatives*, R. A. Royer, T. R. Weeke.

Syracuse

Officers of the Syracuse Section for 1951–52 are as follows: *Chairman*, Harry C. Wolfe; *Vice-Chairman*, E. L. Smith; *Sec.*, Frank R. House, Marcellus, N. Y.; *Treas.*, James Solar; *Chairman, Membership Committee*, E. L. Smith; *Chairman, Program Committee*, S. H. Monson, Jr.; *Technical Representative*, Ed Strand.

Tulsa

Newly elected officers of the Tulsa Section for 1951–52 are as follows: *Chairman*, Ben Walcott; *Vice-Chairman*, John Tipton; *Sec.*, F. L. Silvers; *Treas.*, W. A. Emery; *Chairman, Membership Committee*, R. G. Todd; *Chairman, Program Committee*, M. White; *Technical Representative*, F. Childress.

Washington

The Washington Section announces the election of officers for 1951–52 as follows: *Chairman*, W. P. Roberts, Jr., Southern Oxygen Co.; *Vice-Chairman*, Paul Tarver; *Sec.*, R. H. Evert, Westinghouse Electric Corp.; *Treas.*, R. A. Webster; *Chairman, Membership Committee* Paul Tarver; *Chairman, Program Committee*, C. A. Loomis.

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abstracts of CURRENT WELDING PATENTS

prepared by V. L. Oldham

Printed copies of patents may be obtained for 25¢ from the Commissioner of Patents, Washington 10, D. C.

2,558,005—Wheel Welding Machine and Method—Mario M. Sciaky, Chicago, Ill., assignor to Welding Research, Inc., Chicago, Ill., a corporation of Illinois.

This patent relates to a welding machine wherein a workpiece and a welding electrode are positioned in relatively adjustable relationship. A welding current is adapted to be applied to the electrode to produce the weld and pressure means for applying force to the workpiece to attempt to effect the separation of the weld is provided. The movement of such pressure means beyond a predetermined position is blocked by the weld, while control means for the machine are present and are actuated by the movement of the pressure means beyond the previously referred to predetermined position so that the control means is actuated upon the insufficiency of the weld.

2,558,083—Controller Handle for Electrode Holders—Lawrence O. Green, Cincinnati, Ohio, assignor to The Foster Transformer Co., Cincinnati, Ohio, a corporation of Ohio.

Green's patent relates to a part for use with a welding electrode holder and it has an axial welding cable passage and means at one end for receiving an electrode holder. A rotatable sleeve is provided on the handle member adjacent one end thereof and it has two sets of contact elements within the handles member with contactors secured to the rotatable sleeve. The device is also provided with means for positioning the sleeve in three positions to vary the connection between the contact elements and the control sleeve.

2,558,102—Arc Welding System—William L. Roberts, Pittsburgh, Pa., Assignor to Westinghouse Electric Corp., East Pittsburgh, Pa., a Corporation of Pennsylvania.

This patent relates to a stabilizing sys-

tem for use with an arc circuit in which an arc is to be produced. The system includes an energy storage device and circuit means connecting the energy storage device to receive electrical energy from the arc circuit. Additional circuit means present in the system include a valve device connected to provide a discharge circuit for the energy storage device at predetermined timed intervals so as to produce impulses in the arc circuit.

2,558,360—Method and Means for Electrical Welding Chiefly in the Production of Metal Containers—Edmond Leon Jean Jadoul, Gembloux, Belgium, assignor to Ecremeuses Melotte, Sosiete Anonyme, Remicourt, Belgium, a Belgian Company.

This patent is on an apparatus for electrically welding two parts of a metallic container which container parts interengage each other through cooperating beveled edges and it includes a wheel adapted to engage the outer periphery of the container parts. A disconnectable swage is mounted on the inside of one of the parts to be welded and inwardly supports the insides of the container parts in register with the first-mentioned wheel engaging the outer portion of such cooperating edges. The first mentioned wheel is provided with driving means for frictionally causing the container to rotate and means are provided for feeding an electric welding current through the cooperating beveled edges of the parts to be welded together.

2,558,889—Method and Apparatus for Progressively Electric Resistance Butt Welding Seams at High Speed—Sven Westin, Elm Grove, and Avel Westin, Milwaukee, Wis., assignors to The Westin Process Co., a partnership.

This patent relates to an electric resistance progressive butt welding of substantially straight seams in articles constructed of metal sheet and it includes the application of a resistance heating current across abutting edges of the metal sheet throughout a predetermined zonal length of the seam and application of a sustained welding pressure throughout such zonal length. The flow of heating current across the abutting edges is substantially confined to the zonal length referred to.

2,559,527—Gas Burner and Flame Shield—John Roger Williams, Ambler, Pa., assignor to Selas Corporation of America, Philadelphia, Pa., a corporation of Pennsylvania.

This patent upon a gas burner relates to burner port units including means to produce a long spear type of a flame. The burner units have shields associated herewith and said shields converge toward each other and have a plurality of narrow slots extending toward the converging edges thereof.

2,559,795—Head Shield for Welders—Joseph F. Rakos, Cleveland, Ohio, assignor of one-half to John W. Nelson, Cleveland, Ohio.

This patent relates to a specialized construction of a head shield wherein a slidably mounted member is provided for operating a window in the head shield.

2,560,678—Method of Welding—John R. Wirt, Anderson, Ind., assignor to General Motors Corp., Detroit, Mich., a corporation of Delaware.

Wirt's welding method relates to uniting a wire with another electrically conducting member and it comprises supporting a wire and a member between electrodes connected to a welding current source. One of the electrodes has an electrical connection with the member and the other electrode which is movable has a surface making an acute angle with the wire longitudinally thereof. The method comprises reducing the thickness of the wire and simultaneously spreading it into a larger area contacting surface by causing the movable electrode to squeeze the wire while electric current passes between the electrodes so that the portion being welded is of wedge-like formation tapering from full diameter of the wire to a thin edge and spreading out to increase the area of the bond. The unattached wire immediately adjacent the wedge remains at full diameter.

2,561,480—Welding Torch Igniter—John S. Pflum, Connersville, Ind.

The patent welding torch lighter includes a housing having a high tension coil therein, and a pair of spark-gap electrodes connected to the coil. A bell-shaped reflector is fixed to the housing and the spark-gap forming electrodes are positioned projecting into the open side of the reflector. A torch-supporting member supports the torch nozzle in a position adjacent the spark-gap forming electrodes, and such torch-supporting member is movable to close a switch to close the circuit to the high-tension coil.

2,561,739—Rotary Welding Transformer Structure—Thomas M. Hunter, Short Hills, N. J.

Hunter's rotary welding transformer structure includes a shaft, a secondary winding having inner and outer cylindrically shaped parts at spaced distances from each other, the inner and outer parts being joined at one end by a removable closure plate of suitable current conducting material. Both parts of such material have concentric cylindrically shaped projecting ends insulated from each other and positioned relatively close to the shaft. The inner projecting end has a support on the extremity of the shaft and each projecting end has an electrode disk fastened thereto in cooperative relationship. A primary winding and core are positioned as a readily removable unit between the two first-mentioned parts of the secondary and is removable therefrom when the closure plate is removed. The transformer also includes means having parts carried by the plate for conveying current to the primary winding.

2,561,995—Arc Welding System—William L. Roberts, McKeesport, Pa., assignor to Westinghouse Electric Corp., East Pittsburgh, Pa., a corporation of Pennsylvania.

This patent covers a combination with an arc welding circuit of oscillator means and coupling means connecting the oscillator means with the arc-welding circuit. Additional control means are provided and are responsive in accordance with the impedance of the arc-welding circuit to vary the effective impedance of the coupling means and vary the output impedance of the oscillator means.

2,562,132—Automatic Arc Welding with Coated Electrodes—Willibald Senft, Weiz, near Graz, Austria, assignor to Elin Aktiengesellschaft für elektrische Industrie, Vienna, Austria, a joint-stock corporation of Austria.

This patent covers a special type of an electrode for use in automatic arc welding and particularly relates to electrode-covering material that comprises ready-shaped rigid pieces of flux material of short length and of an arcuate cross section. Substantially continuous means connect the short lengths of flux together in an endwise relationship, which connection means are flexible. Additional means can be fed with the electrode, and are provided for retaining the flux pieces around the electrode core.

2,562,133—Automatic Arc Welding with Coated Electrodes—Willibald Senft, Weiz, near Grax, Austria, assignor to Elin Aktiengesellschaft für elektrische Industrie, Vienna, Austria, a joint-stock corporation of Austria.

This patent relates to the use of the special type of electrode that is covered in Patent No. 2,562,132 and it further relates to a method of arc welding wherein a bare electrode core is fed to the welding head and current is supplied to said electrode core which thereafter is covered with flux material and the covered electrode is fed to the arc. The method includes the step of feeding the flux material special rigid pieces to the electrode core so that such rigid pieces can be assembled thereabout to form a complete covering with a hole inside of a larger diameter than that of the electrode core. Such flux material is fastened together and fed as an electrode covering to the arc by feed means distinct from the means which feed the electrode core to the arc.

WELDING RESEARCH COUNCIL

of the Engineering Foundation

Sponsored by the American Welding Society and American Institute of Electrical Engineers American Institute of Mining and Metallurgical Engineers, American Society of Civil Engineers American Society of Mechanical Engineers, and Society of Naval Architects and Marine Engineers

Supplement to the Welding Journal, September, 1951

Upper and Lower Transitions in Charpy Test

Charpy V-notch impact test shows two transitions in the ductility temperature relationship of mild steel. A testing technique is indicated for studying the energy required for the formation and the propagation of the crack in a V-notch Charpy specimen

by W. J. Harris, Jr., J. A. Rinebolt and R. Raring

Abstract

Charpy V-notch specimens of medium-carbon steels were tested in impact by multiple low-velocity, low-energy blows. Under these modified conditions of test, a discontinuity in the energy-temperature relationship, at temperatures below which the fracture surface was nearly all crystalline, was observed. This low-temperature discontinuity was associated with the occurrence of plastic-strain markings on the sides and top of the specimens.

INTRODUCTION

In 1949 Osborn, Scotchbrook, Stout and Johnston[1] reported results of tests on Lehigh, Kahn, and Schnadt specimens which showed that steel can be considered to have two transition temperatures. They called the higher-temperature transition T_B and concluded that it is most satisfactorily determined by a fracture-appearance criterion and that it represents a transition of cold-worked material under the stress state existing at the root of a sharp crack. The lower-temperature transition they called T_N and concluded that it is most satisfactorily determined by a criterion based on the contraction of width at the notch root and that it represents a transition of undeformed material under the stress-state imposed by the machined notch. Subsequent to the publication of reference 1, the reality of the double transition in steel received further support from data of Stout and McGeady[2,3] and of Enzian and Salvaggio,[4] which demonstrated the effects of welding and of composition on the upper and lower transitions.

The idea that steel has two transition temperatures was received with interest by investigators dealing with the problem of brittle fracture of welded structures, largely because it suggested a possible solution to a problem which has long confronted them. This problem arises from the fact that brittle fractures of structural members differ from "brittle" fractures in most test specimens in that the former is associated with little or no plastic strain while the latter occurs after appreciable plastic strain and energy absorption; thus to identify the two "brittle" fractures was often suspect. Also, the rate of decrease of ductility in test specimens as the temperature is reduced below that at which nearly 100% granular fracture occurred was thought to be so gradual that it would not provide a satisfactorily critical index of an embrittlement temperature. The occurrence of a second critical change in the ductility-temperature relationship of steel at a temperature lower than that at which the fracture face changes from fibrous to nearly all granular affords a possible solution to this problem, viz., the identification of fracture below the lower transition with brittle fracture in structures.

This possibility was discussed by Vanderbeck and Gensamer in an interpretative paper[5] based on exten-

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sive data from notch-bar and wide-plate tests conducted at several laboratories and from the investigation of the National Bureau of Standards on fractured plates from failed ships. One intent of reference 5 was to demonstrate that either the lower transition temperature or, in the case of tests which did not reveal a double transition, a temperature corresponding to some arbitrary low-energy absorption provide a better correlation with service performance than the upper or "fracture appearance" transition temperature.

Table 1—Chemical Composition of Steels in Per Cent

Steel No.	*Thickness, in.*	*C*	*Mn*	*Si*	*P*
1	5/8	0.30	1.05	0.27	0.008
2	5/8	0.28	1.00	0.30	0.008
3	1	0.20	0.38	0.004	0.013
4	1	0.26	0.44	0.17	0.009
5	3/4	0.14	0.82	0.17	0.023
6	3/4	0.16	1.21	0.32	0.022
7	1 1/2	0.15	1.24	0.33	0.022
8	3/4	0.30	0.20	0.00	0.010
9	3/4	0.26	0.47	0.04	0.008
10	3/4	0.18	0.40	0.04	0.011

represent plates removed from ships which suffered brittle fracture in service; Steel 8 represents a "start" plate, while Steels 9 and 10 represent "through" plates.*

Particulars of the composition and the mechanical properties of all steels are listed in Tables 1 and 2.

PROCEDURE

Standard Charpy V-notch specimens were tested in an impact machine in the conventional manner, with the exception that the kinetic energy of the hammer was varied by changing the mass and/or the velocity of the hammer. In order to determine whether or not the energy absorbed in fracturing would be affected by such modifications of the conditions of test, specimens were tested at various temperatures using the velocity-energy combinations listed in Table 3. This table shows that so long as the energy-velocity combination is greater than 10 ft.-lb. at 3.5 ft. per second, the energy ab-

* A "start" plate is one in which a fracture originated; a "through" plate is one through which the fracture passed but in which it neither originated nor terminated.

Table 2—Mechanical Properties

Steel No.	*T. S., psi.*	*Y. P., psi.*	*% Elong. in 2 in.*	*R. A. %*	*Transition temperature, ° F. — 240 ft.-lb. energy, Av. energy*	*15 ft.-lb.*	*25 ft.-lb. energy, lower*
1	80,000	46,000	35.0	60.5	26	−50	−80
2	80,000	52,500	34.5	63.5	10	−60	−90
3	69,000	35,000	33.5	51.5	165	95	105
4	69,000	34,000	35.5	56.5	130	72	70
5	72,000	49,500	31.5	67.0	96	38	12
6	83,000	56,000	31.0	66.5	61	10	−5
7					73	28	10
8	60,900	32,500	35.5	57.0	..	153	150
9	64,500	37,500	38.0	60.0	..	72	90
10	58,000	31,000	37.0	65.0	..	58	45

The present paper presents data which indicate that the Charpy V-notch impact test shows two transitions in the ductility-temperature relationship of mild steel, especially when certain nonstandard conditions of test are employed. It also describes a testing technique by which the energy required for the formation and the propagation of the crack in a V-notch Charpy specimen can be studied, and suggests that the lower transition temperature represents a critical change in the energy absorbed in crack initiation while the upper transition represents an increase in the energy absorbed in propagation of the crack.

MATERIALS

Steels 1 and 2 were from Si-Al killed laboratory heats which were melted in a 300-lb. induction furnace, cast in 65-lb. ingots and forged to 5/8-in. square bars. Specimen blanks were cut from these bars, normalized at 1650° F., reheated to 1650° F. and slowly cooled (25° F./min.) to 800° F. Steels 3 and 4 conformed to Grade M, and Steels 5, 6 and 7 to grade HT, of Navy Dept. Specification 4885 (Hull plate). These steels were of commercial manufacture. Steels 8, 9 and 10

Table 3—Comparison of Energy to Fracture V-Notch Specimens with Single and with Multiple Blows

Steel	*Temp., °F.*	*Energy, ft.-lb.*	*Velocity, ft./sec.*	*Total energy* to fracture*
X†	80	240	17.0	110
X	80	10 + 2nd of 240	3.5 (first)	106
Y†	80	240	17.0	94
Y	80	30 + 2nd of 240	6.0	92
Y	80	60 + 2nd of 240	8.5	90
Y	80	70 + 2nd of 240	9.2	90
Y	80	50 + (8 of 5 + 2 of 10)	7.8	110
Y	80	50 + 5 of 10	7.8	94
Y	80	50 + 3 of 15	7.8	90
Z†	80	80	9.8	69
Z	80	240	17.0	69
1	140	240	17.0	97
1	140	15	4.3	99
1	110	240	17.0	92
1	110	15	4.3	92
1	80	240	17.0	78
1	80	15	4.3	74
1	40	240	17.0	56
1	40	15	4.3	60

* Rebound energies are not deducted.

† Steels X, Y and Z were commercial ship-plate, composition and mechanical properties of which are not available.

sorbed in fracturing by multiple low-energy blows is essentially equal to that absorbed in fracturing by a single 240 ft.-lb., 17 ft. per second standard blow. The elastic energy contained in bars which were not fractured by the low-energy blows caused a slight rebound of the hammer. The value of such elastic energy was estimated by observing the height of the hammer rebound, and in all instances was found to be between 1 and 2 ft.-lb. Since the energy absorption was essentially independent of the energy-velocity combination through the range mentioned, all steels except Steel 1 were tested with a 25 ft.-lb., 11.0 ft. per second combination. This particular combination was selected because it corresponded to a latch position on the machine, while the hammer height for other low-energy, low-velocity combinations had to be determined experimentally and the hammer released from a handhold.

The beginning and extension of the cracks were observed during the tests of Steel 1 (15 ft.-lb., 4.3 ft./sec.) by examining the specimens with a 10 × microscope after each blow of the hammer.

Standard keyhole Charpy tests were conducted on Steels 1, 2, 9 and 10 and the zone of scatter as defined in reference 5 determined.

RESULTS

Lower Transition Temperature

Figures 1 through 6 illustrate the low-temperature discontinuities in the energy-temperature relationship. It is evident from these figures that some steels (e.g., Steel 2, Fig. 2) show a greater and more abrupt energy drop than do others (e.g., Steel 3, Fig. 3). It will be observed that in many instances the lower transition is represented by a more abrupt change of slope than would be justified on the basis of the energy data alone. This practice in drawing the curves was followed in order to represent also the temperature above which certain strain markings, which will be discussed later and which are associated with the lower transition, appear

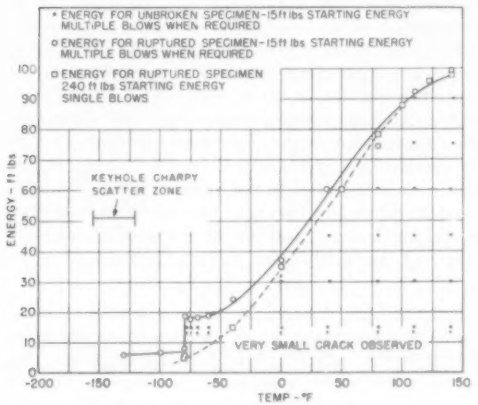
* ENERGY FOR UNBROKEN SPECIMEN-15ft lbs STARTING ENERGY MULTIPLE BLOWS WHEN REQUIRED
o ENERGY FOR RUPTURED SPECIMEN-15ft lbs STARTING ENERGY MULTIPLE BLOWS WHEN REQUIRED
□ ENERGY FOR RUPTURED SPECIMEN 240 ft lbs STARTING ENERGY SINGLE BLOWS
KEYHOLE CHARPY SCATTER ZONE
VERY SMALL CRACK OBSERVED
ENERGY - ft lbs
TEMP - °F

Fig. 1 Transition curve, Steel 1

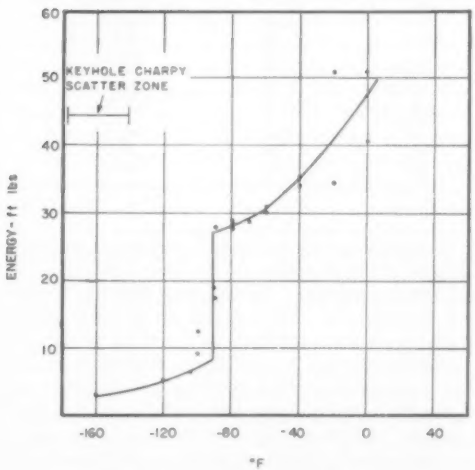

Fig. 2 Transition curve, Steel 2

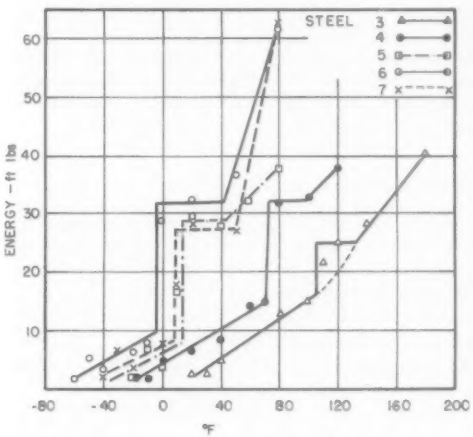

Fig. 3 Lower transition, Steels 3, 4, 5, 6 and 7

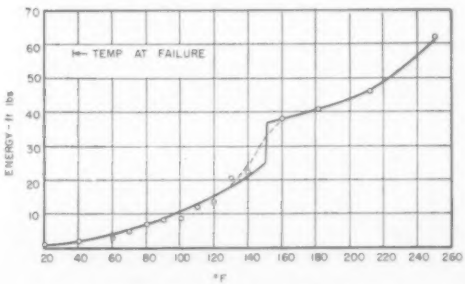

Fig. 4 Transition curve, Steel 8

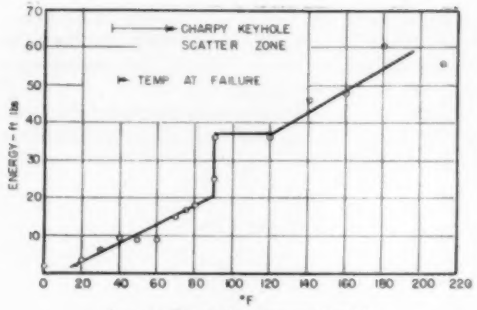

Fig. 5 Transition curve, Steel 9

on the fractured specimens. However, a faired curve through the energy data for all steels, with the possible exception of Steel 3 which was unique in this respect, reveal an energy discontinuity at the lower transition. Such faired curves are indicated by broken lines for Steels 3 and 8.

For purposes of comparison, the temperature range corresponding to the scatter zone in the Charpy keyhole test is indicated on Figs. 1, 2, 5 and 6. This zone appears to occur at or below the lower transition temperature as determined by the Charpy V-notch specimens. Also, on Figs. 4, 5 and 6, which represent steels from fracture ships, the air temperature at the time of the casualty is indicated for information. As can be seen, for two steels (Nos. 8 and 9) the temperature at failure was below the lower transition temperature, while for one steel (No. 10) it was slightly higher.

Strain Markings

It was observed that the lower transition temperature could be readily determined by means of certain strain markings on the surface of the fractured bars; these markings are schematically illustrated by Fig. 7. Above the lower transition, two triangularly shaped zones are delineated by strain markings, while below this temperature only one such zone appears. These

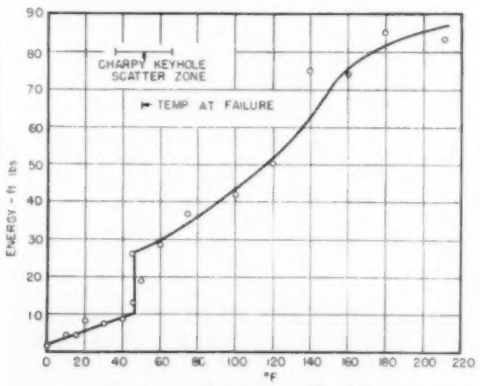

Fig. 6 Transition curve, Steel 10

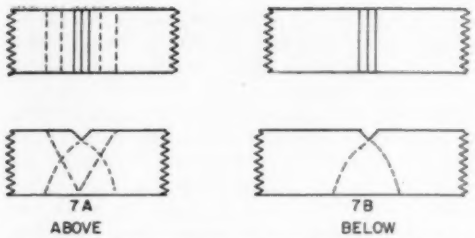

Fig. 7 Characteristic strain markings occurring in V-notch Charpy specimens above and below the lower transition temperature

zones are represented by broken lines on the elevation views. Markings on the top of the broken bars also change critically as the lower transition is passed. Above this temperature, bands of strain marks extend across the bar as shown by the broken lines on the plan view of Fig. 7, but do not occur on bars broken below this temperature.

It is interesting to note that although the standard 240 ft.-lb. 17 ft. per second test does not seem to reveal a lower transition in terms of a critical energy drop as does the lower-energy, lower-velocity technique, still the disappearance of the strain markings does occur in the standard test and at approximately the temperature of the lower transition in the modified test. The temperature ranges in which the strain markings disappear in the standard test and the lower transition as determined by the modified test are listed in Table 4.

Table 4—Lower Transition Temperatures in the Lower-Energy, Lower-Velocity Test and Temperature of Disappearance of Strain Markings in Standard V-Notch Charpy Test

Steel	*Lower transition in nonstandard test, ° F.*	*Range in which marks disappear in standard test, ° F.*
1	−80	−60 to −80
2	−90	−60 to −80
3	105	100 to 140
4	70	67 to 100
5	10	20 to 40
6	−5	−20 to 0
7	10	20
8	150	140 to 150
9	90	60 to 75
10	45	50 to 60

The significance of the strain markings and the relationship which they bear to the lower transition are matters of conjecture. For materials with like moduli and not markedly unlike strains at the yield point, which requirements the steels used in this investigation approximately meet, it is considered that the markings should appear at a unique bend angle of the specimen, provided that the specimen bends without cracking appreciably. However, if a crack develops in the specimen before the bend angle at which the strain markings appear is reached, it seems reasonable to conclude that

the specimen would "bend," as it were, by cracking. Under such conditions, it is considered that fracture could occur before the strain represented by the inverted triangle shown in Fig. 7 (*A*) develops. On the basis of this reasoning, it is concluded that at temperatures above the lower transition, the crack forms only after the occurrence of sufficient plastic bending to develop the strain markings, while below the lower transition the crack forms prior to the development of the strain markings and the crack thus formed allows relief from further strain behind the crack.

If this explanation is correct, it would be expected that by progressively increasing the depth of the notch, a notch depth would be reached at which the strain markings represented by the inverted triangle would not appear even in a fully ductile specimen. To test this, a series of V-notch specimens were made from Steel No. 2 with notch depths increasing in increments of approximately 0.025 in. These specimens were then tested at a sufficiently high temperature to insure a fully fibrous fracture. Results of these tests showed that the strain markings represented by the inverted triangle were clearly evident in bars with notch depths of 0.105 in. and less, but were absent in those with notch depths of 0.125 and more. It is considered that these results tend to verify the above explanation of the strain markings.

Energy for Crack Initiation

In the course of preliminary tests using various striking energies, it was observed that at all temperatures from above the onset of brittleness to the lower transition, the energy required for initiation of a small crack in the notch root was constant. The magnitude of this energy was between 10 and 15 ft.-lb. This finding is illustrated in Fig. 1, which shows that in all specimens struck with a single 15 ft.-lb. blow at a temperature above the lower transition a small crack could be seen in the notch root with a ten-power microscope. The test temperature appeared to have no discernible influence on the size, shape or location of such cracks. Thus it can be considered that for these steels the energy required for initiation of a crack is independent of temperature, as long as the temperature is above the lower transition, and that additional energy absorbed in fracturing the bar is associated with the extension of the crack. It is also evident from Fig. 1 that the lower transition corresponds to a temperature at which an abrupt change in the energy for crack initiation occurs, while the upper transition corresponds to a temperature range in which the energy for crack propagation is increasing.

DISCUSSION

It appears that the conclusions, as stated with introduction, of Osborn, *et al.*, concerning the upper and lower transition temperatures in Lehigh, Kahn, and Schnadt-type specimens are applicable also to Charpy V-notch specimens tested in impact. Thus, as illustrated by Fig. 1, above the lower transition temperature such granular fracture as develops does so from the root of a small crack rather than at the base of a machined notch, while below the lower transition the specimen fractures by cleavage before a small shear crack develops. The conclusion that the upper transition is associated with the change from 100% fibrous fracture to nearly 100% granular applies also to the Charpy V-notch test, since at temperatures immediately above the lower transition the amount of fibrous fracture varies from about 10% to a vanishingly small amount.

In considering the findings of the present paper, the question as to why the modification of the condition of test should result in an apparently increased sensitivity in the detection of the lower transition immediately arises. It is believed that among the reasons for the apparently greater sensitivity of the low-energy technique might be (*a*) the greater variation in the kinetic energy of the broken fragments after brittle fracture of specimen in the higher velocity test, (*b*) the tendency to test relatively few specimens in the temperature range corresponding to nearly 100% granular fracture and (*c*) increased sensitivity concomitant to the closer matching of the striking energy and the absorbed energy.

In regard to the possibility of correlation between the lower transition in the V-notch Charpy test and the occurrence of brittle failure in service, it is considered that the data from tests of steels from failed ships are insufficient to form the basis of any conclusions.

SUMMARY OF FINDINGS

1. Charpy V-notch impact tests of mild steels show a discontinuity in the energy-temperature relationship at temperatures below which the fracture is predominantly granular.

2. Detection of the lower transition temperature is facilitated by reducing the striking energy of the hammer.

3. The lower transition temperature is associated with a critical change in the occurrence of certain characteristic strain markings on the surface of the fractured specimens.

4. At all temperatures above the lower transition, the energy required to initiate a crack is constant while the energy absorbed in extending a crack varies with temperature.

5. The lower transition appears to represent a critical change in the energy for crack formation, while the upper transition is associated with a change in the energy required for crack propagation.

ACKNOWLEDGMENT

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curement of the steel from failed ships, and to W. S. Kenton for conductance of the majority of the tests.

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Residual Stresses in Welded Mild-Steel Pipe

Stresses due to circumferential single-V butt welds in 5½-in. mild-steel pipe, ½ in. thickness, with different welding procedures

by L. J. Privoznik

Abstract

Stresses due to circumferential single-V butt welds, made with five passes in 5½ in. O.D., ½-in. wall thickness mild-steel pipe, were determined by the Sachs boring-out method. The effects of various welding procedures upon the magnitude of stresses, both in the weld and in the parent metal 1¼ in. from the weld, were investigated.

Transverse stresses in the weld were found to be tensile at the inner surface and compressive at the outer surface, with the maximum compressive stresses occurring a short distance below the outer surface. Welding procedures influenced the magnitude of these stresses. Maximum stresses approaching the conventional yield strength of the material, both tensile and compressive, were found in the as-welded, as-received pipe. Stress relieving at 1200° F. for 1 hr. reduced the magnitude of the transverse stresses from 30,000 to 10,000 psi. max. Annealing the pipe prior to welding resulted in transverse stresses lower in magnitude than were found in welded as-received pipe.

Circumferential stresses were found to be less than 11,000 psi. compressive at or near the inner surface and tensile at or near the outer surface. Stress relieving the welded as-received pipe did not affect the magnitude of the circumferential stresses. Annealing the pipe prior to welding resulted in circumferential stresses higher in magnitude than in the welded as-received pipe.

PREVIOUS WORK

RELATIVELY little information is available regarding residual stresses due to circumferential welds in pipe. In several investigations that have been carried out, the circumferential and transverse stresses have been regarded as being independent of each other, and the investigators have limited themselves to surface measurements made by mechanical means.

Ericksen and Wojtaszak[1] investigated residual stresses due to circumferential welds in 20-in. O.D. mild-steel pipe with 1 in. wall thickness and in 8⅝-in. O.D. mild-steel pipe with ½ in. wall thickness. The methods used were limited to measurements of the outer surface and consequently to a state of stress which varied linearly through the thickness. The transverse stresses, therefore, were assumed to be of equal magnitude, but opposite in direction, at the two surfaces.

Circumferential stresses were determined by a method involving two steps: (*a*) the change in diameter of a ring section when it was removed from the pipe, and (*b*) the change in diameter of the ring obtained in step (*a*) when it was split. The changes in diameter in each case were converted to stress and added to give the total circumferential stress.

Transverse stresses were determined by cutting tongues in the pipe, measuring the amount of deflection at the free end of the tongue and converting this value into stress. Maximum transverse compressive stresses of 45,000 to 50,000 psi. in the 20-in. O.D. pipe and 24,000 to 25,000 psi. in the 8⅝-in. O.D. pipe and maximum circumferential tensile stresses of 26,000 to 32,000 psi. in the 20-in. O.D. pipe and 13,000 to 17,000 psi. in the 8⅝-in. O.D. pipe were found. Stress relieving reduced both the maximum transverse stresses (to values of 8500 psi. in the 20-in. O.D. pipe and 5000 psi. in the 8⅝ in. O.D. pipe) and the maximum circumferential stresses (to values of 4000 psi. in the 20-in. O.D. pipe and 3500 psi. in the 8⅝-in. O.D. pipe). Since the values obtained for the larger pipe exceed the yield strength of either the weld metal or the base metal, it appears probable that such high values were obtained largely because of the assumption that strains in different directions relax independently.

Sachs and Graham[2] investigated the residual stresses in butt-welded thin-walled steel tubing of 1¾ in. O.D. and 0.084 in. wall thickness, but their results are of limited applicability because of errors inherent in the method of measurement and the fact that welds in very thin-walled tubing were being investigated. Circumferential stresses were determined by a method similar to that employed by Ericksen and Wojtaszak. The change in circumference of ring sections when they were cut free from the tube were measured by means of a friction disk in contact with the tube. The amount of rotation of the tube necessary to effect a definite degree of rotation of the disk was measured and the change in circumference converted into stress. The change in circumference was attributed only to circumferential

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stresses. The average transverse stresses in the weld were assumed to be negligible since the wall was very thin.

Two types of welded specimens were investigated. The first type had the surface sandblasted prior to making measurements and the second type had the inner and outer surfaces machined (resulting in a removal of about 60% of the weld bead).

The results indicated that a maximum tensile stress of approximately 15,000 psi. occurred at the outer surface of the weld, changing to a compressive stress of the same magnitude in the heat-affected zone of the base metal and decreasing in magnitude with increasing distance from the weld. It was found that the machining operation reduced the magnitude of the stresses in the base metal to a greater extent than it reduced the magnitude of the stresses in the weld.

APPLICATION OF THE SACHS BORING-OUT METHOD

The Sachs boring-out method[3] was selected for this investigation of residual stresses due to circumferential butt welds in mild-steel seamless tubing largely because the distribution of stress in the weld through the wall thickness was not known.

The method is tedious and time-consuming requiring the utmost care and caution in the application of the method and the interpretation of the results. It consists of boring out the interior of the pipe in small increments and measuring changes in strain, by means of SR-4 electric-resistance strain gages, in the circumferential and transverse directions after each boring operation. The conditions ordinarily assumed for this method are that the stress is uniform, both circumferentially and transversely, and that it varies only through the depth. After each boring-out step it is assumed that part of the stress is relieved and part is redistributed uniformly through the remaining thickness. Since the stress distribution in a weld was being determined in this investigation, the assumption was made that the stress need not be uniform along the entire length of the specimen but that it was uniform along the length of the strain gage (a gage length of $^1/_4$ in. was used). The remaining assumptions were unchanged. By using the data obtained from strain-gage measurements, transverse, circumferential and radial stresses can be calculated for the cylinder.

WELDING PROCEDURE

Six pipe specimens, $5^1/_2$ in. O.D., $^1/_2$ in. wall thickness and 13 in. over-all length, were prepared using the welding procedures indicated in Table 1. The pipe used analyzed C 0.13%, Mn 0.45%, P 0.008%, S 0.026% and Si 0.02%.

All welds were made, in a 65-degree included angle single-V groove containing a $^1/_8$-in. root opening with a $^3/_{32}$-in. root face, in five passes with $^5/_{32}$-in. E-6011 electrodes using 140 amp. a. c.

Table 1—Welding Procedures

Specimen No.	*Procedure*
1 and 2	As-received pipe, welded with no preheat or stress relief.
3 and 4	As-received pipe, welded with 400° F. preheat and stress relief of 1 hr. at 1200° F.
5 and 6	Pipe annealed at 1600° F. for 2 hr., then welded with no preheat or stress relief.
7	As-received pipe, with no weld or heat treatment.

A 1-in. wide sleeve was tack welded to each pipe section in order to facilitate clamping the section in the lathe without introducing severe bending moments in the test sections themselves. Stresses due to the tack-welded rings were found to be negligible and had a negligible effect upon the results.

STRAIN MEASUREMENT

SR-4 electric-resistance strain gages (type A-7) were mounted around the circumference of the pipe, both at the weld and $1^1/_4$ in. from the weld. Therefore, there was a set of transverse and circumferential gages mounted 120 degrees apart on all pipe specimens except No. 6 and No. 7. Specimens No. 6 and No. 7 had strain gages mounted 180 degrees apart on a circumference.

A Baldwin SR-4 model K strain indicator was used to obtain the strain-gage readings. Contact between the gage leads and the strain indicator was made with modified alligator-type clip leads in which the hollow contact jaws were filled with brazing metal. Laboratory-temperature variations were compensated for by the use of a dummy gage. All bore dimensions were measured with a micrometer.

During the boring-out process, cutting tools were kept sharp and the depth of cutting at any one time was kept small in order to minimize heating. Temperatures below 120° F. had little if any effect upon the short-time gage stability in the range of strain changes encountered; at temperatures above 120° F. changes in the wax or cement or both had an appreciable effect upon the readings. The evaporation of water from a wet porous cloth wrapped around the pipe specimen kept the temperature well below 120° F. so that there was little if any effect on the short-time gage stability in the range of strains encountered.

For the boring operation, the cross-sectional area of the material was divided into an average of 15 concentric rings with the first and last two rings removed being of smaller cross-sectional area than the remaining rings. After each boring step the lathe jaws were loosened and the pipe allowed to cool until the strain-gage readings became stable. (Under a constant strain of 1200 to 1300 micro inches, using Duco-cement bonded gages, strain-gage readings were found to drift considerably over a period of days. Under a constant strain of 0 to 500 micro inches the readings were found to be the same regardless of whether they were taken after an initial period of about 5 hr. or after a period of several weeks.) During this investigation the complete boring-out

operation required a time of $1^1/_2$ to 3 weeks and the maximum strain change did not exceed 800 micro inches. Readings became virtually stable approximately 5 hr. after boring. Boring steps and readings were made twice a day. For pipe section No. 2, which was a duplicate of No. 1, boring steps and readings were made once a day. Where duplicate specimens were investigated the results were quite reproducible.

STRESS CALCULATIONS

The following equations[5,6] were used for the calculation of stresses by the boring-out method.

$$\sigma_t = \frac{E}{1-\mu^2}\left[(F_b - F)\frac{dA}{dF} - A\right] \quad (1)$$

$$\sigma_c = \frac{E}{1-\mu^2}\left[(F_b - F)\frac{d\theta}{dF} - \frac{(F_b - F)}{2F}\theta\right] \quad (2)$$

$$\sigma_r = \frac{E}{1-\mu^2}\left[\frac{(F_b - F)}{2F}\theta\right] \quad (3)$$

where

F_b = cross-sectional area of circle having same diameter as outside diameter of pipe.
F = cross-sectional area of variable bore (to surface of last borig operation).
σ_t, σ_c, σ_r = respectively, the transverse, circumferential and radial stresses in the cylinder after borning out a cross-sectional area of F.
E = modulus of elasticity (30×10^6 psi.)
μ = Poisson's ratio (0.30)
A = $\epsilon_t + \mu\epsilon_c$
θ = $\epsilon_c + \mu\epsilon_t$
ϵ_t = cumulative strain measured by transverse strain gage after boring.
ϵ_c = cumulative strain measured by circumferential strain gage after boring.

The respective changes in transverse and circumferential strain readings were averaged after each boring-out step before calculating the stresses. As can be seen from Table 4 the deviation of individual readings from the average was low in the case of the transverse strains and relatively high in the case of circumferential strains. The circumferential strain-gage readings appear to deviate considerably because the changes in strain with boring-out were relatively small. However, when the points for strain for each gage were plotted separately, versus area of bore, a relatively smooth curve resulted. The differences in individual gage readings may be due to a number of causes, some of which are nonuniform pipe-wall thickness, variations in thickness of successive welding passes and welding defects. The variations of individual readings from the average as indicated in Table 4 are representative of the other specimens.

Table 2—Strain Measurements for Pipe No. 1 (As-Received As-Welded)

Area of, bore, sq. in.	ϵ_t, *micro inches*	ϵ_c, *micro inches*	A, *micro inches*	θ, *micro inches*
15.4	0	0	0	0
15.77	22	−3	21	4
16.12	56	−15	51	2
16.81	126	−40	114	−2
17.57	204	−61	186	0
18.30	290	−95	261	−8
19.10	388	−125	350	−9
19.83	512	−166	462	−12
20.60	597	−186	541	−7
21.50	749	−235	678	−10
21.80	777	−229	708	+4
22.10	727	−189	670	+29
22.65	443	−84	418	+49

In converting biaxial strain to biaxial stress, Poisson's ratio must be taken into account. Values for A and θ are obtained by correcting the strain measurements for biaxiality.

An example of the method of calculations for pipe No. 1 (as-received, as-welded pipe) is given in Tables 2 and 3 and in Fig. 1. By plotting values of corrected strain (A and θ) against the variable cross-sectional area, F, as is done in Fig. 1, the rate of change of strain with change of cross-sectional area can be obtained by measuring the slope of the curve at any cross-sectional area

$$\left(\frac{dA}{dF} \text{ and } \frac{d\theta}{dF}\right)$$

When calculations involve the measurement of slopes from a curve a certain amount of error, associated with the scatter of points around the fitted curve, always is present. In the case of the data obtained in this investigation, the scatter of points was relatively small although it did become more pronounced as the changes in the measured strain became smaller.

Knowing dA/dF, $d\theta/dF$, A, θ and F, the transverse, circumferential and radial stresses can be calculated

Table 3—Calculation of Stresses for Pipe No. 1 Using the Data in Table 2 and in Fig. 1

F	$F_b - F$	$\frac{dA}{dF}$	A	$(F_b-F)\times\frac{dA}{dF}$	$(F_b-F)\times\frac{dA}{dF} - A$	σ_t	$\frac{d\theta}{dF}$	$(F_b-F)\times\frac{d\theta}{dF}$	θ	$\left(\frac{F_b-F}{2F}\right)\theta$	$(F_b-F)\frac{d\theta}{dF} - \left(\frac{F_b-F}{2F}\right)\theta$	σ_c	σ_r
16.0	7.15	88.3	40	631	591	19,500	0	0	4	0.9	−0.9	−30	29
17.0	6.15	88.3	131	543	412	13,600	−6.5	−40	0	0	−40.0	−1300	0
18.0	5.15	100.0	228	515	287	9,450	−5.4	−27.8	−6	−0.9	−27	−900	−28
19.0	4.15	123.2	337	511	174	5,730	−2.5	−10.5	−10	−1.1	−9	−300	−36
20.0	3.15	135.5	482	412	−70	−2,310	−1.6	5.0	−12	−1.0	−4	−130	−31
21.0	2.15	122.0	605	248	−357	−11,780	2.6	5.6	−12	−0.6	6	200	−20
21.5	1.65	122.0	671	202	−469	−15,450	25.6	42.3	−10	−0.4	43	1400	−13
22.0	1.15	...	...	...	...	...	54.3	62.5	14	−0.4	62	2000	12
22.5	0.65	−667	520	−434	−954	−31,400	55.5	36.1	41	0.6	36	1200	20
22.65	0.50	−667	420	−280	−700	−23,100	...	...	...	...	...	...	...

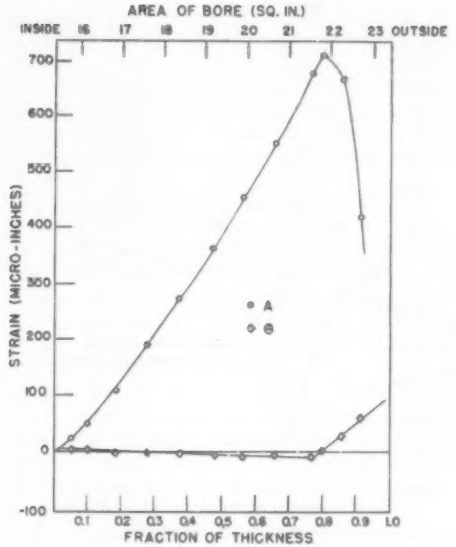

Fig. 1 Change in strain versus area of bore for weld in as-welded, as-received pipe No. 1

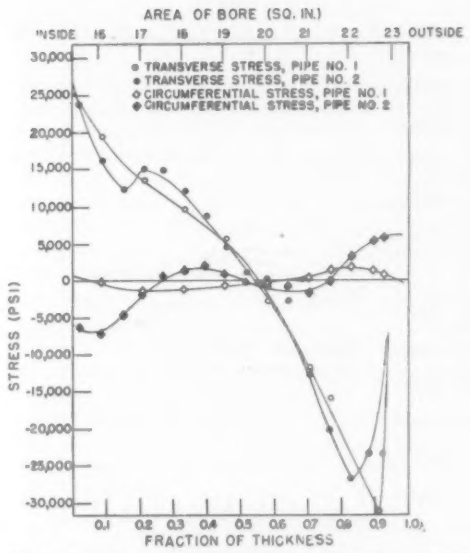

Fig. 2 Average stress distributions in welds of as-welded, as-received pipes Nos. 1 and 2

readily by using equations 1, 2 and 3. These calculations are tabulated in Table 3. Figure 2 shows a plot of the transverse and circumferential stress distributions through the thickness of the pipe wall of pipes No. 1 and No. 2. The radial stresses were found to be negligible so they are not included.

STRESS DISTRIBUTION IN WELDS

Figure 3 shows the transverse and circumferential stress distributions of the weld made with 400° F. preheat and 1200° F. stress-relief postheat, pipe No. 3. Figure 4 shows the stress distributions for pipe No. 5 which, prior to welding, had been annealed at 1600° F. for 2 hrs. and then welded without preheat or stress relief. Figure 5 shows the stress distributions of the as-received pipe No. 7, containing no weld or heat treatment (history of pipe not known).

The changes in strain, with boring, in the band in the pipe, $1^1/_4$ in. from the center line of the weld, were of such low magnitude that determination of the actual stress distributions was highly inaccurate. Therefore, these stress distributions have not been included.

EXPLANATION BASED ON CONCEPT OF WELD AS A SHRUNK METAL BAND

All of the circumferential welds showed essentially the same type of stress distribution. The transverse stresses were tensile at the inner surface changing linearly to compressive at a point near the outer surface and then decreasing in magnitude as the outer surface

Table 4—Comparison of Data for Pipe No. 1

Bore, area, F, in.²	*Average of 3 transverse gages, ϵ_t, micro inches*	*Maximum deviation from average, micro inches*	*Average of 3 circumferential gages, ϵ_c, micro inches*	*Maximum deviation from average, micro inches*	*Transverse stress, psi.*	*Circumferential stress, psi.*
15.4	0	0	0	0	24,000	1400
15.77	22	5	−3	4	21,000	300
16.12	56	6	−15	7	18,600	−500
16.81	126	14	−40	17	14,600	−1300
17.57	204	15	−61	28	11,100	−1200
18.30	290	5	−95	52	8,400	−700
19.10	388	12	−125	80	5,100	−300
19.83	512	22	−166	109	−800	−200
20.60	597	38	−186	121	−8,100	−100
21.50	749	48	−235	118	−16,000	1500
21.80	777	58	−229	114	−19,300	2000
22.27	427	70	−189	56	−31,400	2000
22.65	443	152	−84	44	−23,000	1700
23.15		Extension of curve			−7,000	−1000

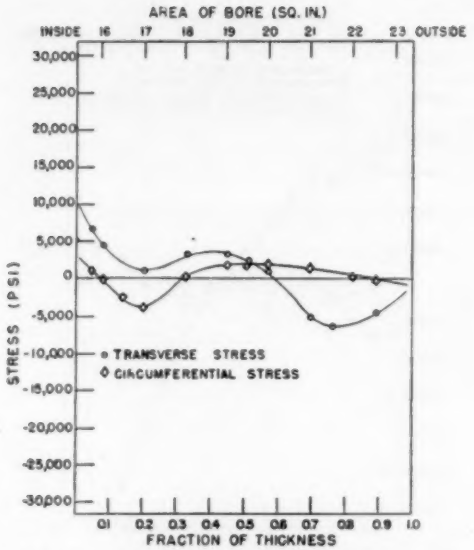

Fig. 3 Average stress distributions in weld made with 400° F. preheat and 1200° F. stress relief postheat, pipe No. 3

was approached. The circumferential stresses were compressive at or near the inner surface and tensile at or near the outer surface. Radial stresses were found to be negligible.

The results can be explained best by considering the weld in the pipe as a narrow band of metal which has been shrunk around the circumference at the mid-point of a pipe section. Upon contracting, the band exerts

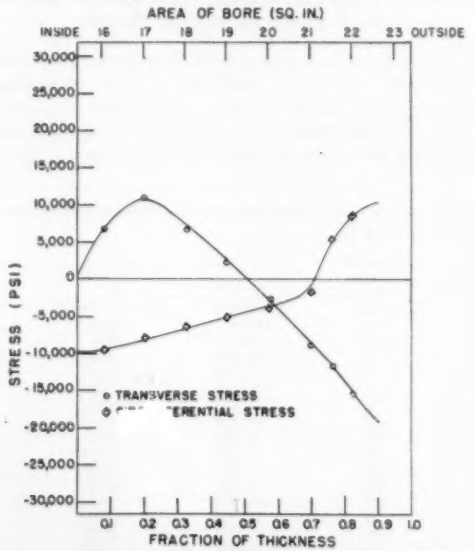

Fig. 4 Average stress distributions in weld made in annealed pipe, pipe No. 5

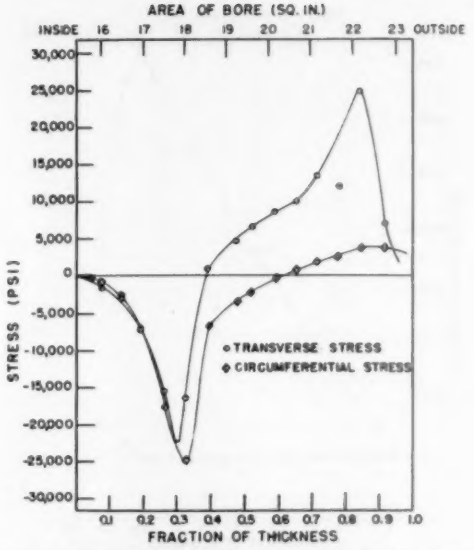

Fig. 5 Average stress distributions in as-received pipe, pipe No. 7

a force F upon the pipe wall as shown in Fig. 6 (*a*) which, in turn, would tend to produce, in the transverse direction, a deflection in the pipe wall with the resulting bending moments, M, and shear forces, S, as shown in Fig. 6 (*b*). This bending moment would result in a maximum tensile stress at the inner surface and a maximum compressive stress at the outer surface directly below the application of the force.

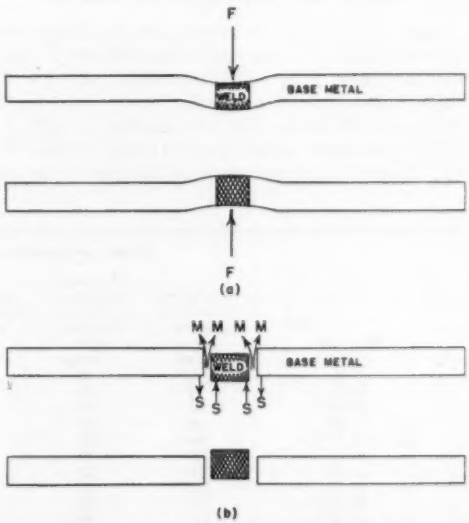

Fig. 6 Schematic view of axial section of pipe (transverse to weld) showing (a) forces, F, exerted by shrinkage of weld metal, and (b) bending moments, M, and shear forces, S, produced by shrinkage of weld metal under restraint of base metal

For the case in which purely elastic bending has occurred and the ring has not shrunk to its minimum diameter, because of the restraint of the pipe, the average circumferential stress will be tensile. If yielding occurs in the pipe wall as the band of metal shrinks, the shear forces, S, will be reduced to a low value and the average circumferential stress will decrease in magnitude. If circumferential shrinkage is complete the average circumferential stress will be zero.

In an actual weld, however, the mode of solidification must have a marked effect upon the circumferential stress distribution. Because initial solidification occurs at the inner surface and final solidification occurs at the outer surface, the inner surface should be in a state of compression and the outer surface in a state of tension. Since final solidification occurs at the outer surface, this also tends to produce a transverse tensile stress at the outer surface. This tensile stress tends to oppose the transverse compressive stress at the surface due to bending, resulting in a transverse stress which is lower in magnitude at the outer surface than it is a short distance below the outer surface.

Since the mild-steel pipe (0.13% carbon) was welded with E-6011 electrodes, the yield strength of the base metal (30,000–35,000 psi.) was lower than that of the weld metal (50,000–60,000 psi.). As it cooled, the band of weld metal tended to shrink until the average circumferential stress became a minimum. This shrinkage produced bending in the transverse direction which probably exceeded the elastic limit of the material causing it to yield (probably in the heat-affected zone). This transverse bending and yielding resulted in a lowering of the average circumferential stress. The fact that stress relieving greatly reduced the transverse stresses but had little or no effect upon the circumferential stresses indicates that the circumferential stresses reached a minimum during the process of welding and were too low in magnitude to be appreciably affected by a stress-relief treatment of 1 hr. at 1200° F.

The magnitudes of the stresses in the weld of the pipe which had been annealed prior to welding were significantly different from those in the weld of the pipe which was welded in the as-welded, as-received condition. The fact that the transverse stresses in the annealed-before-welding specimens were relatively low in magnitude (all-welded specimens had similar distributions) can be attributed to transverse bending occurring over a wider area, thus producing largely elastic bending. This would indicate that shrinkage of the band of weld metal had gone very nearly to completion. If this were the case the average circumferential stress would be expected to be zero or very low. However, the average circumferential stress was found to be predominantly compressive with its maximum value considerably higher than in the as-welded, as-received specimen. This state of stress can only be attributed to progressive solidification of the five passes which constitute the weld. If the stresses resulting from progressive solidification from the inner to the outer surface of a circumferential band of weld metal, which has been restrained from maximum shrinkage during cooling, are ignored, the band will contain an average circumferential tensile stress (magnitude dependent upon degree of restraint). If, however, progressive solidification produces an average circumferential compressive stress in the weld the average circumferential stress due to both restraint and solidification could be very low in magnitude (either compressive or tensile depending upon which factor predominates) even though the band of weld metal has cooled under a relatively high degree of restraint.

Where the degree of restraint is small, therefore, we would expect to obtain an average circumferential stress which is compressive as in the case of the annealed-before-welding specimen. The weld in the as-welded, as-received pipe was probably under considerable restraint, thus accounting for the low magnitude of circumferential stress. However, with the data available positive conclusions about the circumferential stress distribution are not justified.

The high transverse tensile stresses found at the inner surface of a circumferential weld in the as-welded condition should be favorable if the weld were subjected to high internal pressures. These stresses would tend to oppose expansion forces due to internal pressure and to prevent opening of such welding defects as incomplete root penetration.

In stress relieving the weld, the high transverse tensile stresses are considerably lowered in magnitude; so the beneficial effect would be lost.

COMPARISON BETWEEN STRESSES IN FLAT PLATE WELDS AND STRESSES IN PIPE WELDS

A comparison of stresses in circumferential welds in pipe with stresses in welds in flat plate[4, 5] reveals that the two differ significantly both in distribution and in magnitude. Welds made in flat plate generally have high stresses of yield-point magnitude along the length of the weld. Usually these stresses are tensile at both surfaces and these tensile stresses are balanced by compression stresses in the interior. Transverse stresses in flat plate welds are generally of low magnitude, depending upon the amount of restraint, and are tensile at both surfaces, balanced by compression in the interior.

The mechanism by which stresses are produced differ considerably in pipe and flat plate. The type of restraint opposing weld shrinkage is significant in the formation of residual stresses. In flat plate shrinkage occurs largely in a single plane. Since the weld is discontinuous in its length, longitudinal stresses are low near the ends and high in the center. In a circumferential pipe weld, because the weld is continuous, bending occurs during weld shrinkage resulting in low uniform stresses along the length of the weld.

CONCLUSIONS

This study of the distribution and magnitude of stresses due to circumferential butt welds in mild-steel pipe, as determined by Sach's boring-out method, and of the effects of stress relief postheating of the weld and annealing of the pipe prior to welding, has lead to the following conclusions:

1. The as-received pipe, welded without preheat or postheat (stress relief), had high tensile transverse stresses (25,000 to 30,000 psi.) at the inner surface of the weld changing in a roughly linear manner to high compressive stresses (27,000 to 31,000 psi.) a distance approximately $^1/_8$ in. from the outer surface and then dropping sharply in magnitude as the outer surface was approached. The circumferential stresses, after this treatment, were relatively low in magnitude (less than 5000 psi.), compressive at or near the inner surface and tensile at or near the outer surface.

2. Stress relieving at 1200° F. for 1 hr. was effective in reducing the magnitude of the transverse stresses (from 30,000 to 10,000 psi.) but had no effect upon the magnitude of the circumferential stresses.

3. The high transverse tensile stresses at the inner wall of as-welded pipe might be of a favorable nature since they would tend to oppose expansion forces caused by pressure within the pipe. Since stress-relieving considerably lowers these transverse tensile stresses, postheating circumferential welds in mild-steel pipe, from the viewpoint of these stresses alone, might be a harmful rather than a favorable treatment.

4. The transverse stresses in as-welded pipe can be explained as resulting primarily from bending moments caused by shrinkage of the band of weld metal around the circumference, and the circumferential stresses as resulting primarily from the restraint opposing the shrinkage of the band of weld metal.

5. Annealing the pipe prior to welding did not change the stress distributions, but it did result in lower transverse stresses and higher circumferential stresses in the weld after welding than were found in the pipe welded as-received.

6. Both transverse and circumferential stresses, in a band of the base metal $1^1/_4$ in. away from the weld center, were low (less than 5000 psi.) regardless of the welding procedure used. No definite pattern of stress distribution could be established in this zone.

7. Transverse and circumferential stresses in the as-received unwelded pipe were zero or of a very low magnitude at the inner and outer surfaces and reached a maximum compressive magnitude (21,000 psi. transverse and 25,000 psi. circumferential) approximately one-third of the thickness below the inner surface and a maximum tensile magnitude (25,000 psi. transverse and 4000 psi. circumferential) just below the outer surface.

8. Radial stresses were negligible in all pipes and welds.

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Research Institute

Battelle International, the new international research institute to be established in Europe this fall, will maintain laboratories and offices in several Western European countries, it was announced recently. It will, in addition, place research investigations in existing research institutes of Europe and in European universities and technical schools.

The new institute is being launched by Battelle Institute, Columbus, Ohio, the world's largest industrial research foundation. According to Battelle Director Clyde Williams, a three-man team will return to Europe, September 22nd, to complete negotiations for laboratory sites and to recruit a European staff. The team also proposes to launch research projects in the laboratories of such organizations as the Fulmer Research Institute (England), TNO Council (Holland) and the Max Planck Gesellschaft (Germany).

Battelle International proposes to serve the European economy, on a nonprofit basis, through science and technology. It will conduct research in the fields of applied chemistry and physics, metallurgy, fuels, ceramics, electronics, theoretical and applied mechanics, the engineering sciences and agriculture.

As with the parent organization, financial support for the activities of Battelle International will come from industrial sponsorship, both American and European, and from Battelle endowment. Initially it is expected that the program will be financed primarily by Battelle funds and by American industries that have sponsored research at Battelle's Columbus, Ohio, laboratories. Later, it is expected that European industries will supplement financially the initial program.

Dr. Williams estimates that the first year's operating costs probably will exceed $1,500,000. Said Williams: "This money will not be a contribution; it is an investment. We have unlimited confidence in the value of research to industry and we have great faith in the energy and the ability of the European peoples. The benefits to be derived from this program by both the United States and Europe are much too great to pass by the opportunity of meeting this challenge to our ability and our ideals."

An integral part of the Battelle program in Europe is the establishment of truly international laboratories to which scientists and engineers from all of the countries of Europe will be brought to work together. Battelle plans to dispatch teams of scientists from its American laboratories to these international centers to work in these laboratories for limited periods of time. At the same time, scientists from the European laboratories will be brought to the United States to study techniques for industrial research here. Similar exchange arrangements will be offered to other laboratories and research institutes in Europe. In this way the common interests and objectives shared by scientists the world over will be encouraged, and friendship and understanding will be promoted among men of many nations.

Battelle International is the result of three years' study of research in Europe and of the organization of European science and industry. During that period, numerous members of the Battelle staff have studied conditions at first hand. At the same time, Europeans visiting the United States have come to Battelle where these plans and prospects have been discussed with them in detail.

Members of the three-man team scheduled to return to Europe in late September are John S. Crout, who is directing the establishment of Battelle International, Dr. B. D. Thomas and W. R. Keagy. Crout and Thomas are assistant directors of Battelle in America.

The Physical and Metallurgical Characteristics of Spot-Welded Titanium*

Properties and problems of spot welding commercially pure titanium. Optimum current timing and pressures for spot welding titanium are indicated

by M. L. Begeman, F. W. McBee, Jr., and J. C. Fontana

TITANIUM, a relatively new metal, possesses such an unusual combination of physical properties that it is certainly destined to play an important role in our everyday, as well as military, future. This metal has strength near that of steel (though only one-half as heavy), is quite ductile, and melts at 3150° F. Its corrosion resistance ranks with that of platinum and Hastelloy C. The means of fabricating titanium, however, are not too well known. Only recently have investigations of any scope been made on its resistance welding. The information contained in this paper was collected in an effort to examine the properties and problems of spot welding commercially pure titanium.

DESCRIPTION OF EQUIPMENT

The welding in this study was performed on an Acme 75-kva. spot welder, type 3.18.75, style SOAR, using 220-v., 60-cycle, single-phase alternating current. In conjunction with the welder, a model CR 7503-A125 General Electric Thyratron Control Panel was used to control weld time and weld current magnitude. A General Electric Sequence Control Panel, model CR 7503-F, controlled the squeeze time, hold time and off time.

The current in the weld was measured by means of a pointer stop ammeter in conjunction with a current transformer. At the same time a cathode-ray oscillograph was used to observe the current pulsations in the welder.

Weld indentation was measured by means of indicating outside calipers. All of the welds were tested on a Southwark-Tate-Emery 120,000-lb. universal testing machine. The specimens were held in a pair of Templin sheet-metal grips and loaded longitudinally, causing the primary failure to be in shear.

Standard Mallory No. 1527 (Fluted) electrode tips with a 2-in. radius dome were used in making all of the welds. The results obtained, however, indicate that probably a type of truncated cone tip with controlled contact area would give more consistent results, particularly where a long series of welds are to be made.

MATERIAL AND ITS PREPARATION

Commercially pure titanium, purchased from the Titanium Metals Corporation of America and designated Ti-75A, was used in all of the tests performed in this investigation. Physical properties, as annealed, are:

Hardness, Vhn	175
Ultimate strength, psi	70,000–80,000
Yield strength, psi	45,000–55,000
Elongation, %	20–30
Area reduction, %	45–70

This material does not respond to heat treatment, but it may be cold worked to well above 100,000 psi. tensile strength, with a consequent drop in elongation to about 12%. The titanium was supplied in the form of cold-rolled sheet stock of three different gages: 22-gage (0.031-in.), 24-gage (0.026-in.) and 25-gage (0.021-in.).

Prior to welding, the sheet stock was sheared into components $2^{3}/_{4}$ in. long by $^{3}/_{4}$ in. wide. These test specimens were then degreased with carbon tetrachloride; no further cleaning was deemed necessary. In the welding procedure, a lap of $^{3}/_{4}$ in. was used to conform with good practice. In the long series of welds, intermediate spots were made on $^{3}/_{4}$-in. strip stock, while the test specimens were welded as indicated above.

SELECTION OF THE WELDING CONDITIONS

Proper conditions for welding of these specimens were

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selected on the basis of results of previous investigators, as well as previous studies made by the authors. The general rules of good spot-welding technique were followed throughout these tests. Used in making all of the welds tested were these settings: (1) squeeze time, 20 cycles; (2) hold time, 20 cycles; (3) electrode closing rate, 3 ft. per second; (4) throat depth, 12 in.; (5) horn gap, 8 in.; (6) electrode cooling water rate, 1.5 gal. per minute; (7) electrode stroke, 0.25 in.

Electrode-tip pressure or force was considered as a major variable, as were weld current and weld time, with electrode-tip force being considered the least important of the three.

PROCEDURE

The procedure used in welding each of the three different-thickness materials was as follows:

A preliminary test was first made using a tip force of 600 lb. and weld time of 6 cycles, both based on the results of previous investigations, while varying the weld current from 6720 to 18,400 amp. From this data an optimum weld current was selected. Using this weld current, a series of welds was made varying the weld time from 2 to 16 cycles. Combining the above information, optimum weld time and weld current were used to make a series of welds with variable tip pressure and also the limited tip-life tests.

In each case, 3 to 5 welds were made at each point. After fabrication of the welds, the indentation was measured and then the specimens were loaded until failure occurred in the universal testing machine.

In the tip-life tests on a long series of consecutive welds, three test specimens were made at each point on the curve with the intermediate welds being made on strip stock.

RESULTS

Weld Current

Figure 1 shows graphically the results of variable weld current upon the strength of the weld for the three thicknesses of material welded. As the weld current increased, the weld strength increased to a point near 12,000–13,000 amp., after which it remained fairly constant to about 18,000 amp. where expulsion of the metal occurs between the plates. At this point the strength becomes erratic and drops off.

These curves may appear misleading in that the strength of the 24-gage material is approximately equal to that of the 22-gage. This can be explained by the fact that the two materials came from different heats and had different ultimate strengths, the 24-gage material being stronger than the 22-gage. The difference is apparently due to varying amounts of nitrogen and oxygen content of the sheet supplied and to the fact that the strength of the 24-gage material may have been increased in the rolling process.

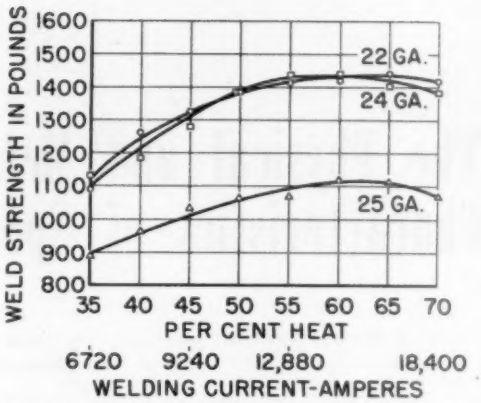

Fig. 1 The effect of variable weld current upon the strength of spot-welded titanium 75A, tip pressure 600 lb., weld time 6 cycles

The fact that satisfactory welds were obtained over the range of, say, 10,000 to 16,000 amp. is of note. This represents a change of 2.5 times as much energy in the latter as in the former, with a consequent change in strength of only about 10% in all three cases.

Optimum conditions were chosen as 12,880 amp. for 22- and 24-gage materials, while 10,860 was chosen as optimum for 25-gage. All of these were at or near maximum strength, but well below the point where flashing begins to occur.

Indentation for this series of welds is shown in Fig. 2. As can be seen, there was a constant increase with increased current. The indentation for all three materials was in the neighborhood of 20% at or near the optimum condition. This is obviously high and can be reduced by using flatter tips with a controlled contact

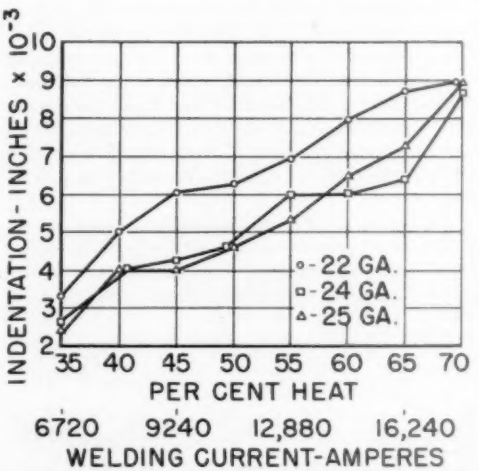

Fig. 2 Influence of weld current upon indentation of spot-welded titanium 75A, weld time 6 cycles, tip pressure 600 lb.

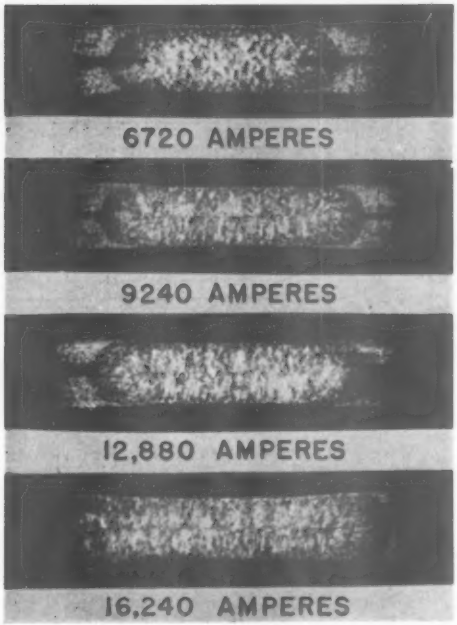

Fig. 3 Influence of current on appearance and size of weld nugget of 22-gage titanium 75A, weld time 6 cycles, tip pressure 600 lb., magnification 10 ×

area. In the event that indentation is of great importance, some strength might well be sacrificed in order to lessen indentation.

In Fig. 3 are four micrographs of typical welds obtained with the 22-gage material which show the influence of various welding currents upon the size and appearance of the nugget. The weld strength for these four conditions, starting with the lowest current, are, respectively, 1087, 1320, 1410 and 1440 lb. The macrographs show very little change in weld penetration (which in all cases exceeds 80%) with a change in weld current. The weld-nugget diameter, however, is quite obviously increased. Sheet separation was normal in all cases and was only slightly increased by higher current values. All welds were free from porosity and appeared to have a good structure.

Weld Time

In Fig. 4 can be seen the results of variable weld time upon the strength of the weld. Here again the strength of the weld increases as the energy into the weld increases. However, the curves are flatter than those for variable weld current. Satisfactory welds were obtained in weld-time ranges of 6 to 14 cycles or a change in energy level of about 2.4 times. At or near 16 cycles flashing again began, with the subsequent result of erratic strength and poor appearance. It is also apparent that the thinner the material the less effect a variation in weld time has on the strength. Optimum weld

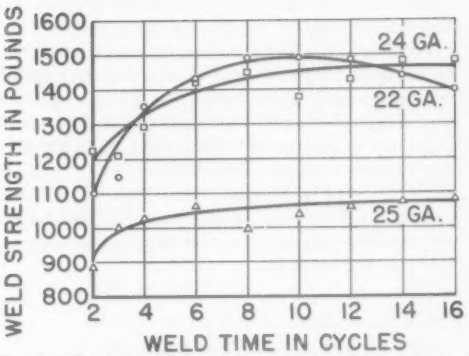

Fig. 4 Effect of varying weld time upon the strength of spot-welded titanium 75A, weld current 12,880 amp. for 22- and 24-gage, 10,860 amp. for 25-gage, all at 600 lb. tip pressure

times were selected as 10 cycles for 22-gage, 6 cycles for 24-gage and 6 cycles for 25-gage.

Figure 5 shows the indentation of these welds with variable weld time. These results are quite similar to those obtained with variable weld current.

The effect of varying the welding time upon the structure and size of the weld is shown in Fig. 6. The diameter of the weld nugget increased directly with a rise in weld time. The increase in indentation can also be seen in these macrographs. As in the case of varying the weld current, penetration was high in all cases, sheet separation normal. The weld made at 10 cycles represents optimum conditions; however, all macrographs represent sound welds.

Tip Pressure

Since the supply of material was limited, the tip pressure was varied on only two materials, i.e., 22-gage and 25-gage. Figure 7 shows the results of these tests made at optimum conditions of 10 cycles weld time and

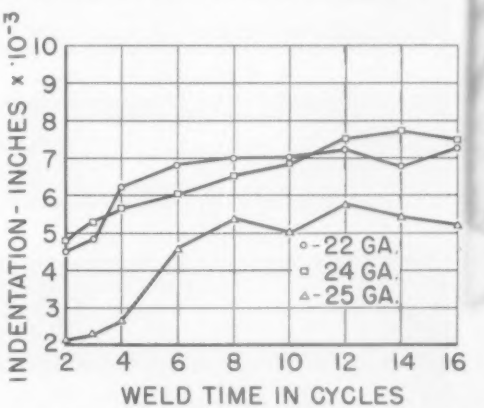

Fig. 5 The result of variable weld time upon indentation in spot welds of titanium 75A, weld current 12,880 amp. for 22- and 24-gage, 10,860 amp. for 25-gage, tip pressure 600 lb. in each case

12,880 amp. for 22-gage material, and 6 cycles weld time and 10,860 amp. for 25-gage material.

Flashing and sticking occurred in each case from 100 to 400 lb.; above this value there was no tendency toward any visible change except that the diameter of the weld nugget increased. As can be seen from the curve, there was an increase in weld strength up to 600 lb. for the 22-gage titanium; higher tip pressures caused a slight decrease in strength. The strength of the 25-gage material was only slightly affected by the change in tip pressure. Indentation, of course, increased with an increase in tip pressure.

Tip-Life Tests

Limited tip-life tests were made on the 22-gage and 24-gage material. The results of these tests are shown in Fig. 6. There was very little change in strength of the weld over the two series. In the case of 24-gage, which appears to be the most erratic, the deviation was only 10% from the average strength in the worst case, weld number 360. In both cases, the average strength of the welds shown on the curve is approximately equal to 85% of the strength of a $^3/_8$-in. strip of the metal used.

There was no sticking or flashing in any case. The only change in the welds from the first to the last was the diameter of the spot, which increased as the tips deformed. Tip deformation was not excessive.

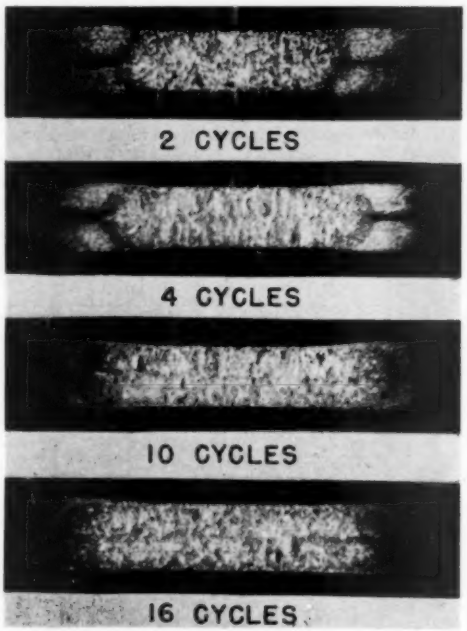

Fig. 6 Influence of time upon appearance and size of weld nugget of 22-gage titanium 75A, welding current 12,880 amp., tip pressure 600 lb., magnification 10 ×

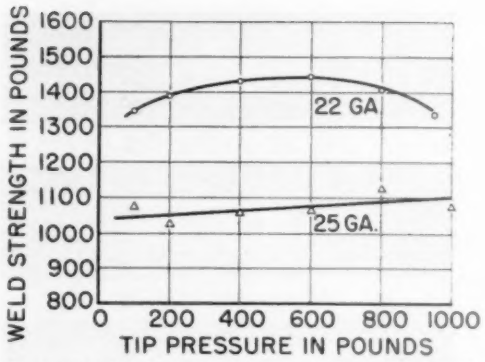

Fig. 7 Variable tip pressure, its effect upon weld strength of 22-gage and 25-gage titanium 75A at optimum conditions of weld time and weld current

Type of Failure

Although the tests performed were to test the weld specimens in shear, failure in all but a few was in a combination of bending and shear. As the load was increased beyond a certain point, a bending action took place which accelerated the failure and resulted in the metal being torn from around the weld nuggets. This type of failure is commonly called a plug failure. Such failures, and those which were a combination of tear around and across the specimen adjacent to the weld, were quite common and indicated high strength.

Hardness of Weld

Because so many of the failures were of the plug type, it was thought advisable to check the hardness across a weld and determine whether or not there had been sufficient hardness developed in the weld nugget to cause embrittlement. In Fig. 9 is shown a hardness study across one of the specimens using a Tukon testing machine with a Vickers diamond pyramid penetrator and a 5-kg. load. From the figure it can be seen that there was a definite increase in hardness in the weld area, but not of sufficient magnitude to cause embrittlement. Greatest hardness was obtained in the area across the center of the nugget. It is possible that the increase in

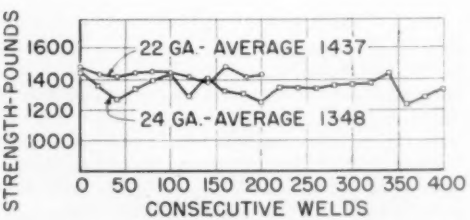

Fig. 8 Log of a series of welds made on titanium 75A at optimum conditions

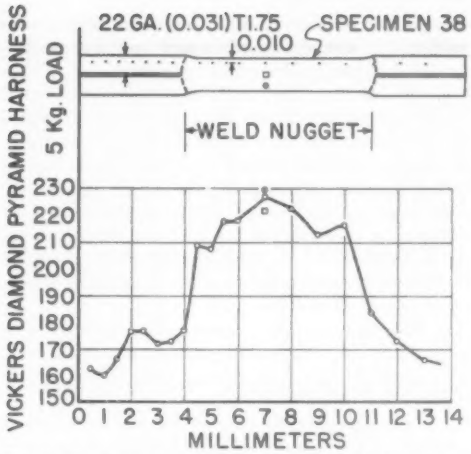

Fig. 9 Hardness study of 22-gage spot-welded titanium 75A, weld current 12,880 amp., weld time 10 cycles, tip pressure 600 lb.

hardness of the welded area was a factor in causing the metal to fail by tearing around the weld.

Further studies were made to determine whether either the weld time or weld current had any appreciable effect upon the weld hardness. Referring again to the specimen shown at the top of Fig. 9, hardness readings were taken at the center of the nugget (indicated by a square) and near one side of the nugget, also on the center line. The results of these tests are shown in the following tabulation:

Specimen No.	*Welding current*	*Welding time*	*—Vickers DPH—* *Edge*	*center*	*Av.*
	Material—Ti 75A 24-gage (0.026-in.)				
565	12,880	6 cycles	272	278	275
564	12,880	12 cycles	244	261	251
563	12,880	16 cycles	281	277	279
	Material—Ti 75A 25-gage (0.021-in.)				
742	10,860	6 cycles	249	274	261
745	12,880	6 cycles	248	265	256
743	14,430	6 cycles	262	247	254

The first three specimens (24-gage material) held the current constant but varied the weld time. Results show a slightly higher hardness at the center than at the edge, but there is no appreciable change in hardness due to changing the weld time. In the last three specimens (25-gage material) the weld current was varied,

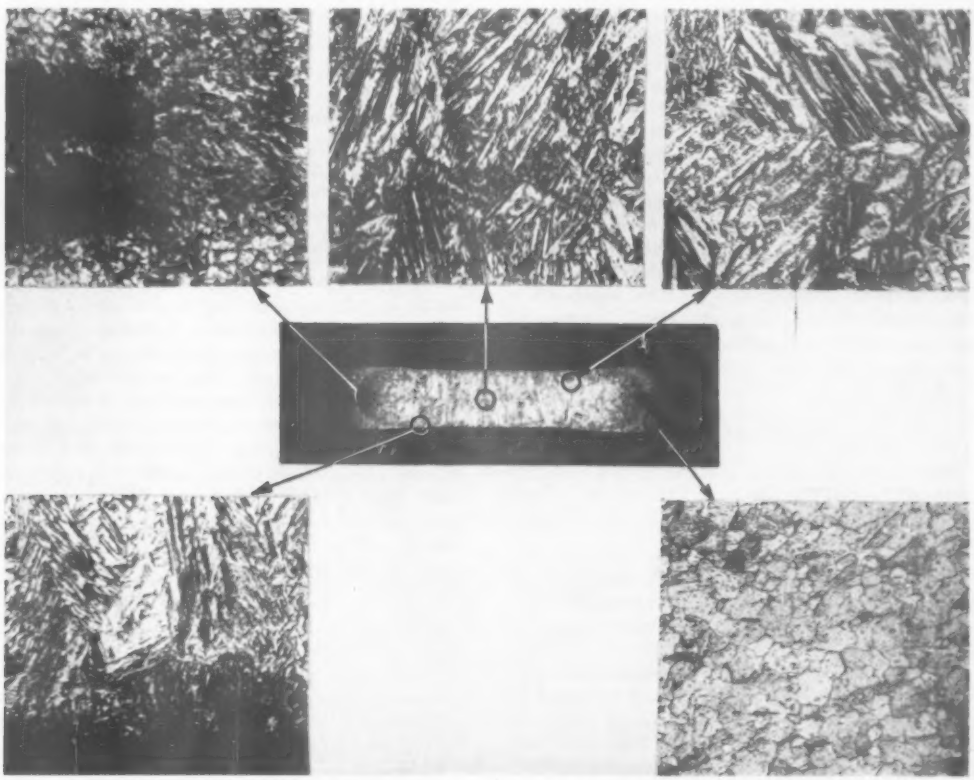

Fig. 10 Spot weld of 22-gage titanium 75A showing microstructure at various locations weld time 8 cycles, weld current 12,880 amp., tip pressure 600 lb., magnification of weld 10 ×, inserts 146 ×

but in general the results were the same. Apparently the hardness is not appreciably affected by either changes of weld time or current.

Structure of Weld

Micrographs of spot welds indicate a normal structure similar to that obtained in the spot welding of steel. Figure 10 shows the microstructure of various locations in a spot weld of 22-gage, titanium 75A. The structures shown are typical of what is to be obtained in a good weld. All grains in the weld nugget are dendritic or columnar and in general are oriented toward the center of the weld. The size of the grains tend to become larger with higher currents and increased welding time, but this tendency is retarded some on thinner sheets as the rate of cooling is increased. Aside from showing the structure, metallographic examinations of specimens also show the size, shape, penetration, location of the weld nugget, as well as the structure adjacent to the weld and the presence or absence of porosity or cracks.

Pure titanium, such as used in this investigation, presents a minimum of metallurgical problems. Since there was only a trace of iron (0.10%) and no other elements present, the structure shown can be considered as made up entirely of titanium. Examination of various welds indicates good bonding is obtained, and there is no evidence of oxidation, porosity or embrittlement. If the temperature or weld time is excessive, or if insufficient tip pressure is used, some flashing will occur, but with proper welding conditions this difficulty is not encountered. In the upper left-hand insert of the figure there is some evidence of a small amount of metal being extruded from the weld. This occasionally happens and is the result of high metal-interface temperature rather than flashing. The structure of the original metal is shown in the lower right-hand insert and is typical of that observed in annealed metal. Subsequent annealing of spot welds will result in this type of structure.

CONCLUSIONS

The following conclusions have been established from the experimental data obtained. They apply specifically to the materials spot welded under the conditions selected.

1. The weld strength of all three thicknesses increases quite uniformly with increases in welding current up to about 16,240 amp. (65% heat); then they decrease gradually.

2. The weld strength of all thicknesses increases to a maximum as the weld time is increased to around 12 cycles; beyond this point the additional time does not appreciably affect the strength, but if continued will ultimately result in a gradual decrease in the strength, and flashing will occur.

3. There is essentially no decrease in the weld strength for a series of 400 consecutive welds. Indentation remained uniform and no pickup was observed on the electrodes.

4. For 22-gage sheet (0.031-in.) there is an increase in weld strength up to 600 lb. tip pressure; for higher tip pressures there is a slight decrease in strength. For lighter materials there is no appreciable change in weld strength as the tip pressure is varied.

5. Welding current values at 18,400 amp. and higher cause excessive flashing and metal expulsion from the weld zone for all thicknesses tested. Excessive flashing also occurs (using normal current values) at low tip pressures up to 400 lb.

6. The indentation increases proportionately with increases in either the welding current or welding time. The effect for increased welding time is not as pronounced as for current increases.

7. The types of failure for all specimens tested were tear around the weld (plug failure) or a combination of tear around and across the specimen adjacent to the weld.

8. The optimum welding conditions for the three thicknesses tested are as follows:

Material	*Welding current, amp.*	*Weld time, cycles*	*Tip pressure, lb.*
22-gage (0.031-in.)	12,880–16,240	8–12	600
24-gage (0.026-in.)	12,880–16,240	6–10	600
25-gage (0.021-in.)	10,880–12,880	6–10	600

9. Satisfactory spot welds can be made on commercially pure titanium over a wide range of conditions.

From the above data it is clear that pure titanium sheet 75A can be effectively fabricated by spot-welding technique; however, compared with low-carbon steel of the same gage, the optimum settings are somewhat different. Although the weld time in cycles is about the same, the welding current required is approximately 50% greater. It was also found advisable to use from 25 to 50% more pressure on the electrode tips for best results. Metallurgical characteristics of the welds appeared to be good and in line with structures obtained when welding other metals. Weld surfaces were symmetrical and free from pits or other defects that would make them appear objectionable. Weld penetration was high and was affected only slightly by current changes and/or weld-time changes. In general, consistent results were obtained in all phases of this investigation.

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Stresses in Large Horizontal Cylindrical Pressure Vessels on Two Saddle Supports

Approximate stresses that exist in cylindrical vessels supported on two saddles at various conditions and design of stiffening for vessels which require it

by L. P. Zick

INTRODUCTION

THE design of horizontal cylindrical vessels with dished heads to resist internal pressure is covered by existing codes. However, the method of support is left pretty much up to the designer. In general the cylindrical shell is made a uniform thickness which is determined by the maximum circumferential stress due to the internal pressure. Since the longitudinal stress is only one-half of this circumferential stress, these vessels have available a beam strength which makes the two-saddle support system ideal for a wide range of proportions. However, certain limitations are necessary to make designs consistent with the intent of the code.

The purpose of this paper is to indicate the approximate stresses that exist in cylindrical vessels supported on two saddles at various locations. Knowing these stresses, it is possible to determine which vessels may be designed for internal pressure alone, and to design structurally adequate and economical stiffening for the vessels which require it. Formulas are developed to cover various conditions, and a chart is given which covers support designs for pressure vessels made of mild steel for storage of liquid weighing 42 lb. per cu. ft.

HISTORY

In a paper[1] published in 1933 Herman Schorer pointed out that a length of cylindrical shell supported by tangential end shears varying proportionately to the sine of the central angle measured from the top of the vessel can support its own metal weight and the full contained liquid weight without circumferential bending moments in the shell. To complete this analysis, rings around the entire circumference are required at the supporting points to transfer these shears to the foundation without distorting the cylindrical shell. Discussions of Schorer's paper by H. C. Boardman and others gave approximate solutions for the half full condition. When a ring of uniform cross section is supported on two vertical posts, the full condition governs the design of the ring if the central angle between the post intersections with the ring is less than 126°, and the half-full condition governs if this angle is more than 126°. However, the full condition governs the design of rings supported directly in or adjacent to saddles.

Mr. Boardman's discussion also pointed out that the heads may substitute for the rings provided the supports are near the heads. His unpublished paper has been used successfully since 1941 for vessels supported on saddles near the heads. His method of analysis covering supports near the heads is included in this paper in a slightly modified form.

Discussions of Mr. Schorer's paper also gave successful and semi-successful examples of unstiffened cylindrical shells supported on saddles, but an analysis is lacking. The semi-successful examples indicated that the shells had actually slumped down over the horns of the saddles while being filled with liquid, but had rounded up again when internal pressure was applied.

Testing done by others[2, 3] gave very useful results in the ranges of their respective tests, but the investigators concluded that analysis was highly indeterminate. In recent years the author has participated in strain gage surveys of several large vessels.[4] A typical test setup is shown in Fig. 1.

In this paper an attempt has been made to produce an approximate analysis involving certain empirical assumptions which make the theoretical analysis closely approximate the test results.

SELECTION OF SUPPORTS

When a cylindrical vessel acts as its own carrying beam across two symmetrically placed saddle supports, one-half of the total load will be carried by each support. This would be true even if one support should

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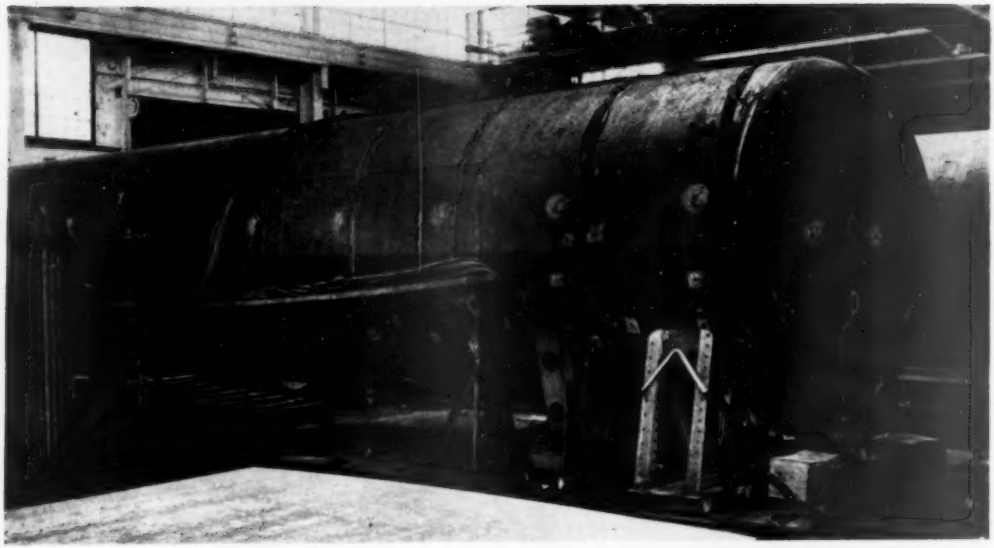

Fig. 1 Strain gage test set up on 30,000 gal. propane tank

settle more than the other. This would also be true if a differential in temperature or if the axial restraint of the supports should cause the vessel acting as a beam to bow up or down at the center. This fact alone gives the two-support system preference over a multiple-support system.

The most economical location and type of support generally depend upon the strength of the vessel to be supported and the cost of the supports, or of the supports and additional stiffening if required. In a few cases the advantage of placing fittings and piping in the bottom of the vessel beyond the saddle will govern the location of the saddle.

The pressure-vessel codes limit the contact angle of each saddle to a minimum of 120° except for very small vessels. In certain cases a larger contact angle should be used. Generally the saddle width is not a controlling factor; so a nominal width of 12 in. for steel or 15 in. for concrete may be used. This width should be increased for extremely heavy vessels, and in certain cases it may be desirable to reduce this width for small vessels.

Thin-wall vessels of large diameter are best supported near the heads provided they can support their own weight and contents between supports and provided the heads are stiff enough to transfer the load to the saddles. Thick-wall vessels too long to act as simple beams are best supported where the maximum longitudinal bending stress in the shell at the saddles is nearly equal to the maximum longitudinal bending stress at mid-span, provided the shell is stiff enough to resist this bending and to transfer the load to the saddles. Where the stiffness required is not available in the shell alone, ring stiffeners must be added at or near the saddles. Vessels must also be rigid enough to support normal external loads such as wind.

Figure 2 indicates the most economical locations and types of supports for large steel horizontal pressure vessels on two supports. A liquid weight of 42 lb. per cu. ft. was used because it is representative of the volatile liquids usually associated with pressure vessels.

Where liquids of different weights are to be stored or where different materials are to be used, a rough design may be obtained from the chart and this design should be checked by the applicable formulas outlined in the following sections. Table I outlines the coefficients to be used with the applicable formulas for various support types and locations. The notation used is listed at the end of the paper under the heading Nomenclature.

MAXIMUM LONGITUDINAL STRESS

The cylindrical shell acts as a beam over the two supports to resist by bending the uniform load of the vessel and its contents. The equivalent length of the vessel (see Figs. 2 and 3) equals $(L + (4H/3))$, closely, and the total weight of the vessel and its contents equals $2Q$. However, it can be shown that the liquid weight in a hemispherical head adds only a shear load at its junction with the cylinder. This can be approximated for heads where $H \leqq R$ by representing the pressure on the head and the longitudinal stress as a clockwise couple on the head shown at the left of Fig. 3. Therefore the vessel may be taken as a beam loaded as shown in Fig. 3; the moment diagram determined by statics is also shown. Maximum moments occur at the mid-span and over the supports.

Tests have shown that except near the saddles a cylindrical shell just full of liquid has practically no circumferential bending moments and therefore behaves as a beam with a section modulus $I/c = \pi r^2 t$.

However, in the region above each saddle circumferential bending moments are introduced allowing the unstiffened upper portion of the shell to deflect, thus making it ineffective as a beam. This reduces the effective cross section acting as a beam just as though the shell were split along a horizontal line at a level above the

Table I—Values of Coefficients in Formulas for Various Support Conditions

Saddle angle, θ	*Maximum long. bending stress, Min. K_1**	*Tangent. shear, K_2*	*Circumf. stress top of saddle, K_3†*	*Additional head stress, K_4*	*Ring compres. in shell, K_5*	*—Ring stiffeners— Circumf. bending, K_6*	*Direct stress, K_7*	*Tension across saddle, K_8*
			Shell unstiffened					
120°	0.63 (A/L = 0.09)	1.171	0.0528	...	0.760	...	...	0.204
150°	0.55 (A/L = 0.11)	0.799	0.0316	...	0.673	...	...	0.260
			Shell stiffened by head, $A \leq R/2$					
120°	1.0 (A/L = 0)	0.880	0.0132	0.401	0.760	...	...	0.204
150°	1.0 (A/L = 0)	0.485	0.0079	0.297	0.673	...	...	0.260
			Shell stiffened by ring in plane of Saddle					
120°	0.23 (A/L = 0.193)	0.319	...	...	...	0.0528	0.340	0.204
150°	0.23 (A/L = 0.193)	0.319	...	...	...	0.0316	0.303	0.260
			Shell stiffened by rings adjacent to saddle					
120°	0.23 (A/L = 0.193)	1.171	...	...	0.760	0.0577	0.263	0.204
150°	0.23 (A/L = 0.193)	0.799	...	...	0.673	0.0353	0.228	0.260

* See Fig. 5, which plots K_1 against A/L, for values of K_1 corresponding to values of A/L not listed in table.
† See Fig. 7.

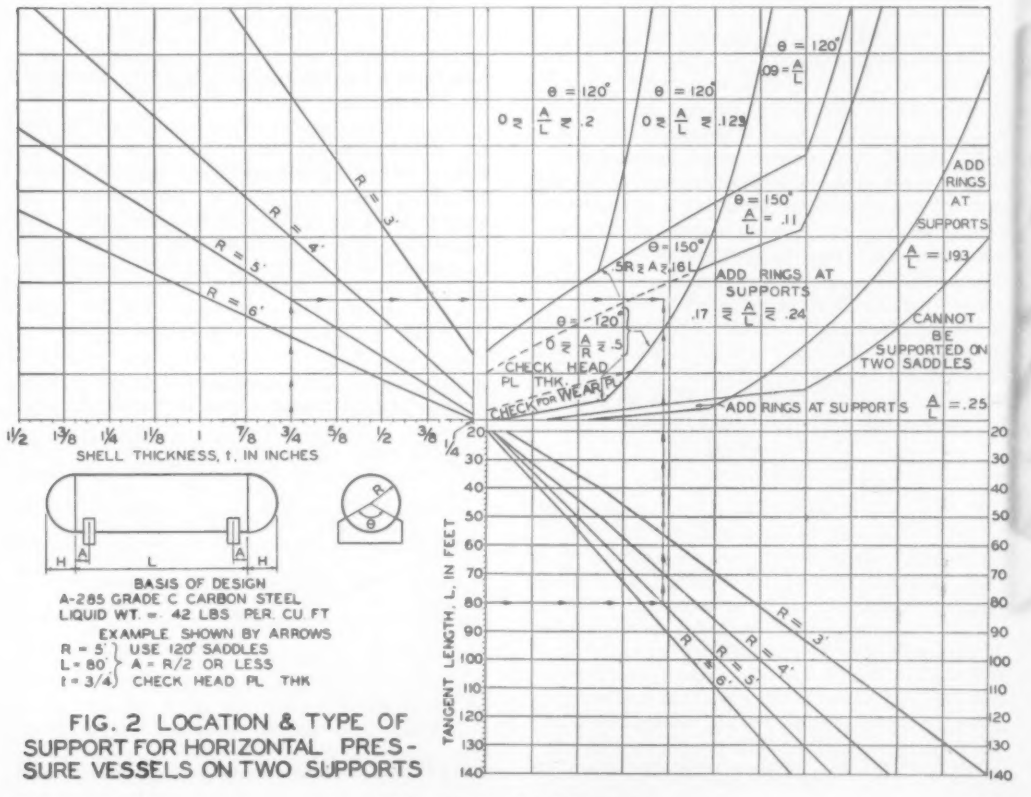
θ = 120°
0 ≤ A/L ≤ .2
θ = 120°
0 ≤ A/L ≤ .129
θ = 120°
.09 = A/L
θ = 150°
A/L = .11
θ = 150°
.5R ≥ A ≥ .16L
ADD RINGS AT SUPPORTS
.17 ≤ A/L ≤ .24
ADD RINGS AT SUPPORTS
A/L = .193
θ = 120°
0 ≤ A/R ≤ .5
CHECK HEAD PL THK.
CHECK FOR WEAR PL
CANNOT BE SUPPORTED ON TWO SADDLES
ADD RINGS AT SUPPORTS A/L = .25
R = 3'
R = 4'
R = 5'
R = 6'
1½ 1⅜ 1¼ 1⅛ 1 ⅞ ¾ ⅝ ½ ⅜ ¼
SHELL THICKNESS, t, IN INCHES
TANGENT LENGTH, L, IN FEET
20 30 40 50 60 70 80 90 100 110 120 130 140
H A L A H
R θ
BASIS OF DESIGN
A-285 GRADE C CARBON STEEL
LIQUID WT. = 42 LBS PER CU FT
EXAMPLE SHOWN BY ARROWS
R = 5' } USE 120° SADDLES
L = 80 } A = R/2 OR LESS
t = 3/4 } CHECK HEAD PL THK

FIG. 2 LOCATION & TYPE OF SUPPORT FOR HORIZONTAL PRESSURE VESSELS ON TWO SUPPORTS

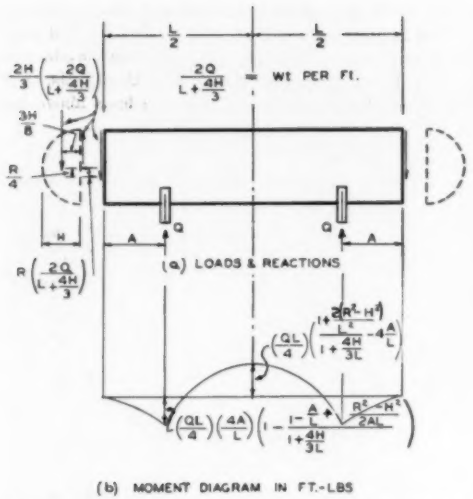

Fig. 3 Cylindrical shell acting as beam over supports

saddle. (See Fig. 4 (*a*).) If this effective arc is represented by 2Δ (Δ in radians) it can be shown that the section modulus becomes:

$$I/c = \pi r^2 t \left(\frac{\Delta + \sin\Delta\cos\Delta - 2\dfrac{\sin^2\Delta}{\Delta}}{\pi\left(\dfrac{\sin\Delta}{\Delta} - \cos\Delta\right)} \right)$$

Strain gage studies indicate that this effective arc is approximately equal to the contact angle plus one-sixth of the unstiffened shell as indicated in Section *A-A* of Fig. 4. Of course, if the shell is stiffened by a head or complete ring stiffener near the saddle the effective arc, 2Δ, equals the entire cross section, and $I/c = \pi r^2 t$.

Since most vessels are of uniform shell thickness, the design formula involves only the maximum value of the longitudinal bending stress. Dividing the maximum moment by the section modulus gives the maximum axial stress in lb. per sq. in. in the shell due to bending as a beam, or

$$S_1 = \pm \frac{3K_1QL}{\pi r^2 t}$$

K_1 is a constant for a given set of conditions, but actually varies with the ratios A/L and $H/L \leq R/L$ for different saddle angles. For convenience, K_1 is plotted in Fig. 5 against A/L for various types of saddle supports, assuming conservative values of $H = 0$ when the mid-span governs and $H = R$ when the shell section at the saddle governs. A maximum value of $R/L = 0.09$ was assumed because other factors govern the design for larger values of this ratio. As in a beam the mid-span governs for the smaller values of A/L and the shell section at the saddle governs for the larger values of A/L; however, the point where the bending

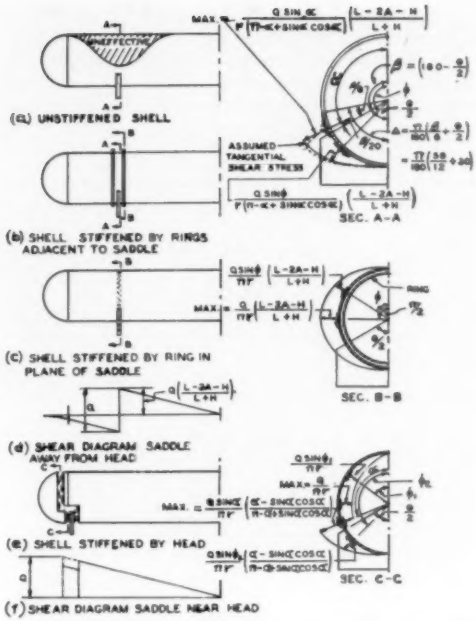
(a) UNSTIFFENED SHELL
UNEFFECTIVE
ASSUMED TANGENTIAL SHEAR STRESS
SEC. A-A
(b) SHELL STIFFENED BY RINGS ADJACENT TO SADDLE
RING
SEC. B-B
(c) SHELL STIFFENED BY RING IN PLANE OF SADDLE
(d) SHEAR DIAGRAM SADDLE AWAY FROM HEAD
(e) SHELL STIFFENED BY HEAD
SEC. C-C
(f) SHEAR DIAGRAM SADDLE NEAR HEAD

Fig. 4 Load transfer to saddle by tangential shear stresses in cylindrical shell

stress in the shell is equal at mid-span and at the saddle varies with the saddle angle because of the reduced effective cross section.

This maximum bending stress, S_1, may be either tension or compression. The tension stress when combined with the axial stress due to internal pressure should not exceed the allowable tension stress of the material times the efficiency of the girth joints. The compression stress should not exceed one half of the compression yield point of the material or the value given by

$$S_1 \leq \left(\frac{E}{29}\right)(t/r)[2 - (2/3)(100)(t/r)]$$

which is based upon the accepted formula for buckling of short steel cylindrical columns. The compression

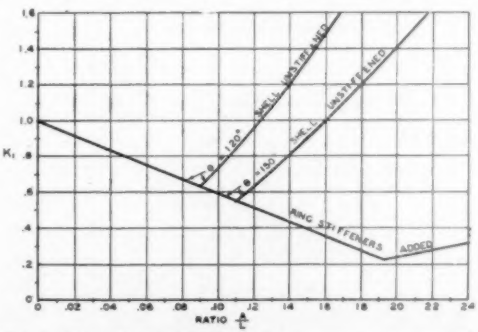

Fig. 5 Plot of longitudinal bending-moment constant, K_1

stress is not a factor in a steel vessel where $t/r \leqq 0.005$ and the vessel is designed for internal pressure.

TANGENTIAL SHEAR STRESS

Figure 4 (*d*) shows the total shear diagram for vessels supported in saddles away from the heads.

Where the shell is held round, the tangential shearing stresses vary directly with the sine of the central angle ϕ, as shown in Section *B-B* of Fig. 4, and the maximum occurs at the equator.

However, if the shell is free to deform above the saddle, the tangential shearing stresses act on a reduced effective cross section and the maximum occurs at the horn of the saddle. This is approximated by assuming the shears continue to vary as the sin ϕ but only act on twice the arc given by $(\theta/2 + \beta/20)$ or $(\pi - \alpha)$ as shown in Section *A-A* of Fig. 4. The summation of the vertical components of these assumed shears must equal the maximum total shear.

The maximum tangential shear stress will occur on the center side of the saddle provided the saddle is beyond the influence of the head but not past the quarter point of the vessel. Then with saddles away from the heads the maximum shear stress in lb. per sq. in. is given by

$$S_2 = \frac{K_2 Q}{rt}\left(\frac{L - 2A - H}{L + H}\right)$$

Values of K_2 listed in Table I for various types of supports are obtained from the expressions given for the maximum shears in Fig. 4.

Figure 4 (*f*) indicates the total shear diagram for vessels supported on saddles near the heads. In this case the head stiffens the shell in the region of the saddle. This causes most of the tangential shearing stress to be carried across the saddle to the head, and then the load is transferred back to the head side of the saddle by tangential shearing stresses applied to an arc slightly larger than the contact angle of the saddle. Section *C-C* of Fig. 4 indicates this shear distribution; that is, the shears vary as the sin ϕ and act downward above angle α and act upward below angle α. The summation of the downward vertical components must balance the summation of the upward vertical components. Then with saddles at the heads the maximum shear stress in lb. per sq. in. is given by

$$S_2 = \frac{K_2 Q}{rt}$$

in the shell, or

$$S_2 = \frac{K_2 Q}{rt_h}$$

in the head.

Values of K_2 given in Table I for different size saddles at the heads are obtained from the expression given for the maximum shear stress in Section *C-C* of Fig. 4.

The tangential shear stress should not exceed 0.8 of the allowable tension stress.

CIRCUMFERENTIAL STRESS AT HORN OF SADDLE

In the plane of the saddle the load must be transferred from the cylindrical shell to the saddle. As was pointed out in the previous section the tangential shears adjust their distribution in order to make this transfer with a minimum amount of circumferential bending and distortion. The evaluation of these shears was quite empirical except for the case of the ring stiffener in the plane of the saddle. Evaluation of the circumferential bending stresses is even more difficult.

Starting with a ring in the plane of the saddle, the shear distribution is known. The bending moment at any point above the saddle may be computed by any of the methods of indeterminate structures. If the ring is assumed uniform in cross section and fixed at the horns of the saddles, the moment, M_ϕ, in in.-lb. at any point A is given by:

$$M_\phi = \frac{Qr}{\pi}\left\{\cos\phi + \frac{\phi}{2}\sin\phi - \frac{3}{2}\frac{\sin\beta}{\beta} + \frac{\cos\beta}{2} - \frac{1}{4}\left(\cos\phi - \frac{\sin\beta}{\beta}\right)\times\left[9 - \frac{4 - 6\left(\frac{\sin\beta}{\beta}\right)^2 + 2\cos^2\beta}{\frac{\sin\beta}{\beta}\cos\beta + 1 - 2\left(\frac{\sin\beta}{\beta}\right)^2}\right]\right\}$$

This is shown schematically in Fig. 6. Note that β must be in radians in the formula.

The maximum moment occurs when $\phi = \beta$. Substituting β for ϕ and K_6 for the expression in the brackets divided by π, the maximum circumferential bending moment in in.-lb. is

$$M_\beta = K_6 Qr$$

When the shell is supported on a saddle and there is no ring stiffener the shears tend to bunch up near the horn of the saddle, so that the actual maximum circumferential bending moment in the shell is considerably less than M_β as calculated above for a ring stiffener in the plane of the saddle. The exact analysis is not known; however, stresses calculated on the assumption

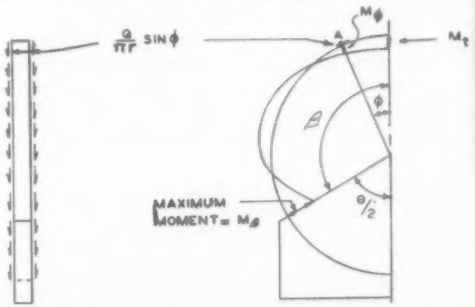

Fig. 6 Circumferential bending-moment diagram, ring in plane of saddle

that a wide width of shell is effective in resisting the hypothetical moment, M_β, agree conservatively with the results of strain gage surveys. It was found that this effective width of shell should be equal to 4 times the shell radius or equal to one-half the length of the vessel, whichever is smaller. It should be kept in mind that use of this seemingly excessive width of shell is an artifice whereby the hypothetical moment M_β is made to render calculated stresses in reasonable accord with actual stresses.

When the saddles are near the heads, the shears carry to the head and are then transferred back to the saddle. Again the shears tend to concentrate near the horn of the saddle. Because of the relatively short stiff members this transfer reduces the circumferential bending moment still more.

To introduce the effect of the head the maximum moment is taken as

$$M_\beta = K_2 Q r$$

Where K_2 equals K_6 when A/R is greater than 1. Values of K_2 are plotted in Fig. 7 using the assumption that this moment is divided by four when A/R is less than 0.5.

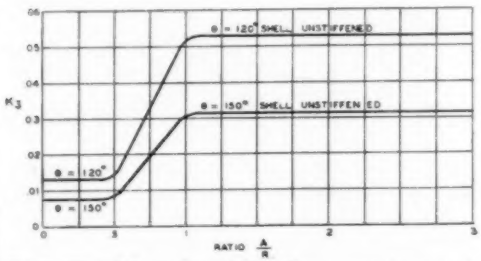

Fig. 7 Plot of circumferential bending-moment constant, K_2

The change in shear distribution also reduces the direct load at the horns of the saddle; this is assumed to be $Q/4$ for shells without added stiffeners. However, since this load exists, the effective width of the shell which resists this direct load is limited to that portion which is stiffened by the contact of the saddle. It is assumed that $5t$ each side of the saddle acts with the portion directly over the saddle.

Internal pressure stresses do not add directly to the local bending stresses, because the shell rounds up under pressure. Therefore the maximum circumferential combined stress in the shell is compressive, occurs at the horn of the saddle, and is due to local bending and direct stress. This maximum combined stress in lb. per sq. in. is given by

$$S_3 = -\frac{Q}{4t(b + 10t)} - \frac{3K_2Q}{2t^2}, \quad \text{if } L \geq 8R$$

or

$$S_3 = -\frac{Q}{4t(b + 10t)} - \frac{12K_2QR}{Lt^2}, \quad \text{if } L < 8R$$

It seems reasonable to allow this combined stress to be equal to 1.25 times the tension allowable provided the compressive strength of the material equals the tensile strength. In the first place when the region at the horn of the saddle yields, it acts as a hinge, and the upper portion of the shell continues to resist the loads as a two-hinged arch. There would be little distortion until a second point near the equator started to yield. Secondly, if rings are added to reduce this local stress, a local longitudinal bending stress occurs at the edge of the ring under pressure.[5] This local stress would be 1.8 times the design ring stress if the rings were infinitely rigid. Weld seams in the shell should not be located near the horn of the saddle where the maximum moment occurs.

EXTERNAL LOADS

Long vessels with very small t/r values are susceptible to distortion from unsymmetrical external loads such as wind. It is assumed that vacuum relief valves will be provided where required; so it is not necessary to design against a full vacuum. However, experience indicates that vessels designed to withstand 1 lb. per sq. in. external pressure can successfully resist external loads encountered in normal service.

Assume the external pressure is 1 lb. per sq. in. in the formulas used to determine the sloping portion of the external pressure chart in the 1950 A.S.M.E. Unfired Pressure Vessel Code. Then when the vessel is unstiffened between the heads, the maximum length in feet between stiffeners (the heads) is given approximately by

$$L + {}^2/_3H = \frac{E\sqrt{rt}}{52.2}\left(\frac{t}{r}\right)^2$$

When ring stiffeners are added to the vessel at the supports, the maximum length in feet between stiffeners is given by

$$L - 2A = \frac{E\sqrt{rt}}{52.2}\left(\frac{t}{r}\right)^2$$

ADDITIONAL STRESS IN HEAD USED AS STIFFENER

When the head stiffness is utilized by placing the saddle close to the heads, the tangential shear stresses cause an additional stress in the head which is additive to the pressure stress. Referring to Section *C-C* of Fig. 4, it can be seen that the tangential shearing stresses have horizontal components which would cause varying horizontal tension stresses across the entire height of the head if the head were a flat disk. The real action in a dished head would be a combination of ring action and direct stress; however, for simplicity the action on a flat disk is considered reasonable for design purposes.

Assume that the summation of the horizontal components of the tangential shears is resisted by the vertical cross section of the flat head at the center line,

and assume that the maximum stress is 1.5 times the average stress. Then the maximum additional stress in the head in lb. per sq. in. is given by

$$S_4 = \frac{3Q}{8rt_h}\left(\frac{\sin^2 \alpha}{\pi - \alpha + \sin \alpha \cos \alpha}\right)$$

or

$$S_4 = \frac{K_4 Q}{rt_h}$$

This stress should be combined with the stress in the head due to internal pressure. However, it is recommended that this combined stress be allowed to be 25% greater than the allowable tension stress because of the nature of the stress and because of the method of analysis.

WEAR PLATES—RING COMPRESSION IN SHELL OVER SADDLE

Figure 8 indicates the saddle reactions, assuming the surfaces of the shell and saddle are in frictionless contact without attachment. The sum of the assumed tangential shears on both edges of the saddle at any point A is also shown in Fig. 8. These forces acting on the shell band directly over the saddle cause ring compression in the shell band. Since the saddle reactions are radial, they pass through the center O. Taking moments about point O indicates that the ring compression at any point A is given by the summation of the tangential shears between α and ϕ.

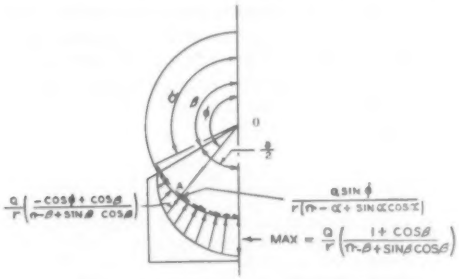

Fig. 8 Loads and reactions on saddles

This ring compression is maximum at the bottom, where $\phi = \pi$. Again a width of shell equal to $5t$ each side of the saddle plus the width of the saddle is assumed to resist this force. Then the stress in lb. per sq. in. due to ring compression is given by

$$S_5 = \frac{Q}{t(b + 10t)}\left(\frac{1 + \cos \alpha}{\pi - \alpha + \sin \alpha \cos \alpha}\right)$$

or

$$S_5 = \frac{K_5 Q}{t(b + 10t)}$$

The ring compression stress should not exceed one-half of the compression yield point of the material.

The stress may be reduced by attaching a wear plate some what larger than the surface of the saddle to the shell directly over the saddle. The thickness t may be taken as the combined thickness of the shell and the wear plate in the formulas for S_4 and S_5 provided the width of the added plate equals $(b + t)$ and provided the plate extends $r/10$ in. above the horn of the saddle.

DESIGN OF RING STIFFENERS

When the saddles must be located away from the heads and when the shell alone cannot resist the circumferential bending, ring stiffeners should be added at or near the supports. Because the size of rings involved does not warrant further refinement, the formulas developed in this paper assume that the added rings are continuous with a uniform cross section. The ring stiffener must be attached to the shell, and the portion of the shell reinforced by the stiffener plus a width of shell equal to $5t$ each side may be assumed to act with each stiffener.

When n stiffeners are added directly over the saddle as shown in Fig. 4 (*c*), the tangential shear distribution is known. The equation for the resulting bending moment at any point was developed previously, and the resulting moment diagram is shown in Fig. 6. The maximum moment occurs at the horn of the saddle and is given in in.-lb. for each stiffener by

$$M_\beta = K_6 \frac{Qr}{n}$$

Knowing the maximum moment M_β and the moment at the top of the vessel, M_t, the direct load at the point of maximum moment may be found by statics. Then the direct load at the top of the saddle is given in pounds by

$$P_\beta = \frac{Q}{\pi n}\left[\frac{\beta \sin \beta}{2(1 - \cos \beta)} - \cos \beta\right] + \frac{\cos \beta}{r(1 - \cos \beta)}(M_\beta - M_t)$$

or

$$P_\beta = K_7 \frac{Q}{n}$$

If n stiffeners are added adjacent to the saddle as shown in Fig. 4 (*b*), the rings will act together and each will be loaded with shears distributed as in Section *B-B* on one side but will be supported on the saddle side by a shear distribution similar to that shown in Section *A-A*. Conservatively, the support may be assumed to be tangential and concentrated at the top of the saddle. This is shown schematically in Fig. 9; the resulting bending moment diagram is also indicated. This bending moment in in.-lb. at any point A above the top of the saddle is given by

$$M_\phi = \frac{Qr}{2\pi n}\left\{\frac{\pi - \beta}{\sin \beta} - \phi \sin \phi - \cos \phi \left[3/2 + (\pi - \beta) \cot \beta\right]\right\}$$

For the range of saddle angles considered M_ϕ is maximum near the equator where $\phi = \rho$. This moment and the direct stress may be found using a procedure similar to that used for the stiffener in the plane of the saddle. Substituting ρ for ϕ and K_6 for the expression in the brackets divided by 2π, the maximum moment in each ring adjacent to the saddle is given in in.-lb. by

$$M_\rho = K_6 \frac{Qr}{n}$$

Knowing the moments M_ρ and M_t, the direct load at ρ may be found by statics and is given by

$$P_\rho = \frac{Q}{\pi n}\left[\frac{\rho \sin \rho}{2(1 - \cos \rho)} - \cos \rho\right] - \frac{\cos \rho}{r(1 - \cos \rho)}(M_\rho + M_t)$$

or

$$P_\rho = K_7 \frac{Q}{n}$$

Then the maximum combined stress due to liquid load in each ring used to stiffen the shell at or near the saddle is given in lb. per sq. in. by

$$S_6 = -\frac{K_7 Q}{na} \pm \frac{K_6 Q r}{nI/c}$$

where a = the area and I/c = the section modulus of the cross section of the composite ring stiffener. When a ring is attached to the inside surface of the shell directly over the saddle or to the outside surface of the shell adjacent to the saddle, the maximum combined stress is compression at the shell. However, if the ring is attached to the opposite surface, the maximum combined stress may be either compression in the outer flange due to liquid or tension at the shell due to liquid and internal pressure.

The maximum combined compression stress due to liquid should not exceed one-half of the compression yield point of the material. The maximum combined tension stress due to liquid and pressure should not exceed the allowable tension stress of the material.

DESIGN OF SADDLES

Each saddle should be rigid enough to prevent the separation of the horns of the saddle; therefore the saddle should be designed for a full water load. The horn of the saddle should be taken at the intersection of the outer edge of the web with the top flange of a steel saddle. The minimum section at the low point of either a steel or concrete saddle must resist a total force, F, in pounds, equal to the summation of the horizontal components of the reactions on one-half of the saddle. Then

$$F = Q\left[\frac{1 + \cos \beta - 1/2 \sin^2 \beta}{\pi - \beta + \sin \beta \cos \beta}\right] = K_8 Q$$

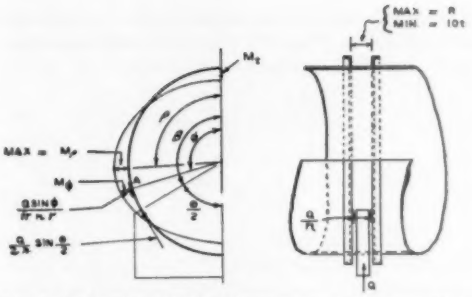

Fig. 9 Circumferential bending-moment diagram, stiffeners adjacent to saddle

The effective section resisting this load should be limited to the metal cross section within a distance equal to $r/3$ below the shell. This cross section should be limited to the reinforcing steel within the distance $r/3$ in concrete saddles. The average stress shoule not exceed two-thirds of the tension allowable of the material. A low allowable stress is recommended because the effect of the circumferential bending in the shell at the horn of the saddle has been neglected.

The upper and lower flanges of a steel saddle should be designed to resist bending over the web(s), and the web(s) should be stiffened according to the A.I.S.C. Specifications against buckling. The contact area between the shell and concrete saddle or between the metal saddle and the concrete foundation should be adequate to support the bearing loads.

Where extreme movements are anticipated or where the saddles are welded to the shell, bearings or rockers should be provided at one saddle. Under normal conditions a sheet of elastic waterproof material at least $^1/_4$ in. thick between the shell and a concrete saddle will suffice.

Nomenclature

Q = load on one saddle, lb. Total load = $2Q$.
L = tangent length of the vessel, ft.
A = distance from center line of saddle to tangent line, ft.
H = depth of head, ft.
R = radius of cylindrical shell, ft.
r = radius of cylindrical shell, in.
t = thickness of cylindrical shell, in.
t_h = thickness of head, in.
b = width of saddle, in.
F = force across bottom of saddle, lb.
S_1, S_2, etc. = calculated stresses, lb. per sq. in.
K_1, K_2, etc. = dimensionless constants for various support conditions.
M_ϕ, M_ρ, etc. = circumferential bending moment due to tangential shears, in.-lb.
θ = angle of contact of saddle with shell, degrees.
$\beta = \left(180 - \frac{\theta}{2}\right)$ = central angle from vertical to horn of saddle, in degrees (except as noted).
$\Delta = \frac{\pi}{180}\left(\frac{\theta}{2} + \frac{\beta}{6}\right) = \frac{\pi}{180}\left(\frac{5\theta}{12} + 30\right)$. 2Δ = arc, in radians, of unstiffened shell in plane of saddle effective against bending.

$\alpha = \pi - \frac{\pi}{180}\left(\frac{\theta}{2} + \frac{\beta}{20}\right)$ = the central angle, in radians, from the vertical to the assumed point of maximum shear in unstiffened shell at saddle.

ϕ = any central angle measured from the vertical, in radians.

ρ = central angle from the upper vertical to the point of maximum moment in ring located adjacent to saddle, in radians.

E = modulus of elasticity of material, lb. per sq. in.

I/c = section modulus, in.^{3}

n = number of stiffeners at each saddle.

a = cross-sectional area of each composite stiffener, sq. in.

P_ρ, P_β = the direct load in lb. at the point of maximum moment in a stiffening ring.

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Appendix

The formulas developed by outline in the text are developed mathematically here under headings corresponding to those of the text. The pertinent assumptions and statements appearing in the text have not been repeated.

MAXIMUM LONGITUDINAL STRESS

Referring to Fig. 3, the bending moment in ft.-lb. at the saddle is

$$\frac{2Q}{L + \frac{4H}{3}}\left[\frac{2HA}{3} + \frac{A^2}{2} - \frac{R^2 - H^2}{4}\right] = QA\left[1 - \frac{1 - \frac{A}{L} + \frac{R^2 - H^2}{2AL}}{1 + \frac{4H}{3L}}\right]$$

Referring to Section *A-A* of Fig. 4 the centroid of the effective arc $= r\,\frac{\sin\,\Delta}{\Delta}$. If δ equals any central angle measured from the bottom, the moment of inertia is

$$2r^3t\int_0^\Delta\left(\cos^2\delta - 2\cos\delta\,\frac{\sin\,\Delta}{\Delta} + \frac{\sin^2\,\Delta}{\Delta^2}\right)d\delta =$$

$$2r^3t\left[1/2\,\sin\,\delta\,\cos\,\delta + \frac{\delta}{2} - \frac{2\sin\,\delta\,\sin\,\Delta}{\Delta} + \frac{\sin^2\,\Delta}{\Delta^2}\delta\right]_0^\Delta =$$

$$r^2t\left[\sin\,\Delta\,\cos\,\Delta + \Delta - 2\frac{\sin^2\,\Delta}{\Delta}\right]$$

The section modulus for the tension side of the equivalent beam is

$$r^2t\left[\frac{\Delta + \sin\,\Delta\,\cos\,\Delta - 2\frac{\sin^2\,\Delta}{\Delta}}{\frac{\sin\,\Delta}{\Delta} - \cos\,\Delta}\right]$$

Then the stress in the shell at the saddle in lb. per sq. in. is given by

$$S_1 = \frac{3QL}{\pi r^2 t}\left[\frac{4A}{L}\left(1 - \frac{1 - \frac{A}{L} + \frac{R^2 - H^2}{2AL}}{1 + \frac{4H}{3L}}\right)\times \frac{\pi\left(\frac{\sin\,\Delta}{\Delta} - \cos\,\Delta\right)}{\Delta + \sin\,\Delta\,\cos\,\Delta - 2\frac{\sin^2\,\Delta}{\Delta}}\right]$$

or

$$S_1 = \frac{3K_1QL}{\pi r^2 t}$$

where

$$K_1 = \left[\frac{\pi\left(\frac{\mathrm{Sin}\,\Delta}{\Delta} - \cos\,\Delta\right)}{\Delta + \sin\,\Delta\,\cos\,\Delta - 2\frac{\sin^2\,\Delta}{\Delta}}\right]\times \left[\frac{4A}{L}\left(1 - \frac{1 - \frac{A}{L} + \frac{R^2 - H^2}{2AL}}{1 + \frac{4H}{3L}}\right)\right]$$

The bending moment in ft.-lb. at the mid span is

$$\frac{2Q}{L + \frac{4H}{3}}\left[\frac{(L - 2A)^2}{8} - \frac{2HA}{3} - \frac{A^2}{2} + \frac{R^2 - H^2}{4}\right] = \frac{QL}{4}\left(\frac{1 + 2\frac{R^2 - H^2}{L^2}}{1 + \frac{4H}{3L}} - 4\frac{A}{L}\right)$$

The section modulus $= \pi r^2 t$, and

$$S_1 = \frac{3K_1QL}{\pi r^2 t}$$

where

$$K_1 = \left(\frac{1 + 2\frac{R^2 - H^2}{L^2}}{1 + \frac{4H}{3L}} - 4\frac{A}{L}\right)$$

Tangential Shear Stress

Section *B-B* of Fig. 4 indicates the plot of the shears adjacent to a stiffener. The summation of the vertical components of the shears on each side of the stiffener must equal the load on the saddle Q. Referring to Fig. 4 (*d*) the sum of the shears on both sides of the stiffener at any point is $Q\sin\phi/\pi r$. Then the summation of the vertical components is given by

$$2\int_0^\pi \frac{Q\sin^2\phi}{\pi r}\,r d\phi = \frac{2Q}{\pi}\left[\frac{\phi}{2} - \frac{\sin\,\phi\,\cos\,\phi}{2}\right]_0^\pi = Q$$

The maximum shear stress occurs at the equator when $\sin\phi = 1$ and $K_2 = 1/\pi = 0.319$.

Section *A-A* of Fig. 4 indicates the plot of the shears

in an unstiffened shell. Again this summation of the vertical components of the shears on each side of the saddle must equal the load on the saddle. Then the total shear at any point is

$$\frac{Q \sin \phi}{r(\pi - \alpha + \sin \alpha \cos \alpha)}$$

and the summation of the vertical components is given by

$$2\int_{\alpha}^{\pi} \frac{Q \sin^2 \phi}{r(\pi - \alpha + \sin \alpha \cos \alpha)}\, rd\phi = Q\left[\frac{\phi - \sin \phi \cos \phi}{\pi - \alpha + \sin \alpha \cos \alpha}\right]_{\alpha}^{\pi} = Q$$

The maximum shear occurs where $\phi = \alpha$ and

$$K_2 = \frac{\sin \alpha}{\pi - \alpha + \sin \alpha \cos \alpha}$$

Section *C-C* of Fig. 4 indicates the shear transfer across the saddle to the head and back to the head side of the saddle. Here the summation of the vertical components of the shears on arc α acting downward must equal the summation of the vertical component of the shears on the lower arc $(\pi - \alpha)$ acting upward. Then

$$2\int_{0}^{\alpha} \frac{Q \sin^2 \phi_1}{\pi r}\, rd\phi_1 = 2\int_{\alpha}^{\pi} \frac{Q \sin^2 \phi_2}{\pi r}\left[\frac{\alpha - \sin \alpha \cos \alpha}{\pi - \alpha + \sin \alpha \cos \alpha}\right] rd\phi$$

or

$$\frac{2Q}{\pi}\left[\frac{\phi_1}{2} - \frac{\sin \phi_1 \cos \phi_1}{2}\right]_{0}^{\alpha} = \frac{2Q}{\pi}\left[\frac{\alpha - \sin \alpha \cos \alpha}{\pi - \alpha + \sin \alpha \cos \alpha}\right]\left[\frac{\phi_2}{2} - \frac{\sin \phi_2 \cos \phi_2}{2}\right]_{\alpha}^{\pi}$$

Finally

$$\frac{Q}{\pi}(\alpha - \sin \alpha \cos \alpha) = \frac{Q}{\pi}(\alpha - \sin \alpha \cos \alpha)$$

The maximum shear occurs where $\phi_2 = \alpha$ and

$$K_3 = \frac{\sin \alpha}{\pi}\left[\frac{\alpha - \sin \alpha \cos \alpha}{\pi - \alpha + \sin \alpha \cos \alpha}\right]$$

CIRCUMFERENTIAL STRESS AT HORN OF SADDLE

See under the heading Design of Ring Stiffeners.

Additional Stress in Head Used as Stiffener

Referring to Section *C-C* of Fig. 4, the tangential shears have horizontal components which cause tension across the head. The summation of these components on the vertical axis is

$$\int_{0}^{\alpha} \frac{Q}{\pi r} \sin \phi_1 \cos \phi_1 rd\phi_1 - \int_{\alpha}^{\pi} \frac{Q}{\pi r} \sin \phi_2 \cos \phi_2 \left[\frac{\alpha - \sin \alpha \cos \alpha}{\pi - \alpha + \sin \alpha \cos \alpha}\right] rd\phi_2 =$$

$$\frac{Q}{\pi}\left\{\left[\frac{\sin^2 \phi_1}{2}\right]_{0}^{\alpha} - \left[\frac{\alpha - \sin \alpha \cos \alpha}{\pi - \alpha + \sin \alpha \cos \alpha}\right]\left[\frac{\sin^2 \phi_2}{2}\right]_{\alpha}^{\pi}\right\} = \frac{Q}{2}\left(\frac{\sin^2 \alpha}{\pi - \alpha + \sin \alpha \cos \alpha}\right)$$

Then assuming this load is resisted by $2rt_h$ and that the maximum stress is 1.5 times the average

$$S_4 = \frac{K_4 Q}{rt_h}$$

where

$$K_4 = \frac{3}{8}\left(\frac{\sin^2 \alpha}{\pi - \alpha + \sin \alpha \cos \alpha}\right)$$

WEAR PLATES

The ring compression at any point in the shell over the saddle is given by the summation of the tangential shears over the arc $= (\phi - \alpha)$ shown in Section *A-A* or *C-C* of Fig. 4 or in Fig. 8. Then

$$-\int_{\alpha}^{\phi} \frac{Q \sin \phi_2}{\pi r}\left(\frac{\alpha - \sin \alpha \cos \alpha}{\pi - \alpha + \sin \alpha \cos \alpha}\right) rd\phi_2 - \int_{\alpha}^{\phi} \frac{Q \sin \phi_1}{\pi r}\, rd\phi_1 = -\int_{\alpha}^{\phi} \frac{Q \sin \phi_2 d\phi_2}{(\pi - \alpha + \sin \alpha \cos \alpha)} =$$

$$-\left[\frac{Q}{\pi - \alpha + \sin \alpha \cos \alpha}\right]\left[\cos \phi_2\right]_{\alpha}^{\phi} = Q\left[\frac{-\cos \phi + \cos \alpha}{\pi - \alpha + \sin \alpha \cos \alpha}\right]$$

The ring compression becomes a maximum in the shell at the bottom of the saddle. Or if $\phi = \pi$ this expression becomes

$$Q\left[\frac{1 + \cos \alpha}{\pi - \alpha + \sin \alpha \cos \alpha}\right]$$

Then

$$K_5 = \left[\frac{1 + \cos \alpha}{\pi - \alpha + \sin \alpha \cos \alpha}\right]$$

DESIGN OF RING STIFFENERS

Stiffener in Plane of Saddle

Referring to Fig. 6, the arch above the horns of the saddle resists the tangential shear load. Assuming this arch fixed at the top of the saddles, the bending moment may be found using column analogy.

If the arch is cut at the top, the static moment at any point A is

$$M_s = \frac{Qr}{\pi}\int_{0}^{\phi} (\sin \phi_1 - \sin \phi_1 \cos \phi_1 \cos \phi - \sin^3 \phi_1 \sin \phi) d\phi_1 = \frac{Qr}{\pi}\left[-\cos \phi_1 - \frac{\cos \phi}{2} \sin^2 \phi_1 + \frac{1}{2} \sin \phi \sin \phi_1 \cos \phi_1 - \frac{\phi_1 \sin \phi}{2}\right]_{0}^{\phi} =$$

$$\frac{Qr}{\pi}\left[1 - \cos \phi - \frac{\phi}{2} \sin \phi\right]$$

Then the M_s/EI diagram is the load on the analogous column.

The area of this analogous column is

$$a_1 = 2\int_0^\beta \frac{r}{EI}\,d\phi = \frac{2\beta r}{EI}$$

The centroid is sin β/β r, and the moment of inertia about the horizontal axis is

$$I_h = 2\int_0^\beta \left(\cos\phi - \frac{\sin\beta}{\beta}\right)^2 \frac{r^3}{EI}\,d\phi =$$

$$\frac{2r^3}{EI}\left[\frac{1}{2}\sin\phi\cos\phi + \frac{1}{2}\phi - \frac{2\sin\phi\sin\beta}{\beta} + \frac{\phi\sin^2\beta}{\beta^2}\right]^\beta =$$

$$\frac{r^3}{EI}\left[\sin\beta\cos\beta + \beta - \frac{2\sin^2\beta}{\beta}\right]$$

The load on the analogous column is

$$q = 2\int_0^\beta \frac{M_s}{EI}\,rd\phi = \frac{2Qr^2}{\pi EI}\int_0^\beta \left(1 - \cos\phi - \frac{\phi}{2}\sin\phi\right)d\phi$$

$$q = \frac{2Qr^2}{\pi EI}\left[\phi - \sin\phi - \frac{\sin\phi}{2} + \frac{\phi\cos\phi}{2}\right]_0^\beta =$$

$$\frac{Qr^2}{\pi EI}\left[2\beta - 3\sin\beta + \beta\cos\beta\right]$$

The moment about the horizontal axis is

$$M_h = -2\int_0^\beta \frac{M_s}{EI}\left(\cos\phi - \frac{\sin\beta}{\beta}\right)r^2 d\phi =$$

$$-\frac{Qr^3}{\pi EI}\int_0^\beta \left(2\cos\phi - 2\cos^2\phi - \phi\sin\phi\cos\phi - \frac{\sin\beta}{\beta}(2 - 2\cos\phi - \phi\sin\phi)\right)d\phi =$$

$$-\frac{Qr^3}{\pi EI}\left[2\sin\phi - \cos\phi\sin\phi - \phi - \frac{\sin\phi\cos\phi}{4} + \frac{\phi}{4} - \frac{\phi\sin^2\phi}{2} - \frac{\sin\beta}{\beta}(2\phi - 2\sin\phi - \sin\phi + \phi\cos\phi)\right]_0^\beta =$$

$$\frac{Qr^3}{\pi EI}\left[\frac{9}{4}\sin\beta\cos\beta + \frac{3}{4}\beta - \frac{3\sin^2\beta}{\beta} + \frac{\beta\sin^2\beta}{2}\right]$$

Then the indeterminate moment is

$$Mi = \frac{q}{a_1} - \frac{M_h Y}{I_h} = \frac{Qr}{\pi}\left\{\frac{2\beta - 3\sin\beta + \beta\cos\beta}{2\beta} - \frac{Y}{4r}\left[\frac{9\beta\sin\beta\cos\beta + 3\beta^2 - 12\sin^2\beta + 2\beta^2\sin^2\beta}{\beta\sin\beta\cos\beta + \beta^2 - 2\sin^2\beta}\right]\right\}$$

The distance from the neutral axis to point A is given by

$$Y = \left(\cos\phi - \frac{\sin\beta}{\beta}\right)r$$

Finally, the combined moment is given by

$$M_\phi = -M_s + M_i = \frac{Qr}{\pi}\left\{\cos\phi + \frac{\phi}{2}\sin\phi - \frac{3}{2}\frac{\sin\beta}{\beta} + \frac{\cos\beta}{2} - 1/4\left(\cos\phi - \frac{\sin\beta}{\beta}\right)\times\left[9 - \frac{4 - 6\left(\frac{\sin\beta}{\beta}\right)^2 + 2\cos^2\beta}{\frac{\sin\beta}{\beta}\cos\beta + 1 - 2\left(\frac{\sin\beta}{\beta}\right)^2}\right]\right\}$$

This is the maximum when $\phi = \beta$; then

$$M_\beta = \frac{Qr}{\pi}\left\{\frac{\beta\sin\beta}{2} - \frac{3}{4}\cos\beta + \frac{3}{4}\frac{\sin\beta}{\beta} + \left(\cos\beta - \frac{\sin\beta}{\beta}\right)\left[\frac{4 - 6\left(\frac{\sin\beta}{\beta}\right)^2 + 2\cos^2\beta}{\frac{\sin\beta\cos\beta}{\beta} + 1 - 2\left(\frac{\sin\beta}{\beta}\right)^2}\right]\right\}$$

Finally $\qquad M_\beta = K_6 Qr$

Because of symmetry the shear stress is zero at the top of the vessel; therefore, the direct load in the ring at the top of the vessel, P_t, may be found by taking moments on the arc β about the horn of the saddle. Then

$$(1 - \cos\beta)rP_t = \frac{Qr}{\pi}\left[1 - \cos\beta - \frac{\beta}{2}\sin\beta\right] - (M_\beta - M_t)$$

$$P_t = \frac{Q}{\pi}\left[1 - \frac{\beta\sin\beta}{2(1 - \cos\beta)}\right] - \frac{1}{r(1 - \cos\beta)}(M_\beta - M_t)$$

The direct load, P_β, at $\phi = \beta$, the point of maximum moment may be found by taking moments about the center. Then

$$r(P_\beta + P_t) = \frac{Qr}{\pi}(1 - \cos\beta) - (M_\beta - M_t)$$

Substituting the value above for P_t and solving for P_β gives

$$P_\beta = \frac{Q}{\pi}\left[\frac{\beta\sin\beta}{2(1 - \cos\beta)} - \cos\beta\right] + \frac{\cos\beta}{r(1 - \cos\beta)}(M_\beta - M_t)$$

or $\qquad P_\beta = K_7 Q$

where

$$K_7 = \frac{1}{\pi}\left[\frac{\beta\sin\beta}{2(1 - \cos\beta)} - \cos\beta\right] + \frac{\cos\beta}{Qr(1 - \cos\beta)}(M_\beta - M_t)$$

If the rings are adjacent to the saddle, K_6 and K_7 may be found in a similar manner, except that the static structure would become the entire ring split at the top and loaded as indicated in Fig. 9.

Design of Saddles

The summation of the horizontal components of the radial reactions on one-half of the Saddle shown in Fig. 8 must be resisted by the saddle at $\phi = \pi$. Then this horizontal force is given by

$$F = \int_\beta^\pi \frac{Q(-\cos\phi\sin\phi + \cos\beta\sin\phi)}{r(\pi - \beta + \sin\beta\cos\beta)}\,rd\phi =$$

$$Q\left[\frac{-1/2\sin^2\phi - \cos\phi\cos\beta}{\pi - \beta + \sin\beta\cos\beta}\right]_\beta^\pi =$$

$$Q\left[\frac{1 + \cos\beta - 1/2\sin^2\beta}{\pi - \beta + \sin\beta\cos\beta}\right]$$

Then $\qquad K_8 = \dfrac{1 + \cos\beta - 1/2\sin^2\beta}{\pi - \beta + \sin\beta\cos\beta}$

The bending at the horn would change the saddle reaction distribution, and increase this horizontal force

The Effect of Fabrication Processes on Steels Used in Pressure Vessels*

Notch toughness of steel plate which had been prepared by machining, flame cutting or shearing

by S. S. Tör, J. M. Ruzek and R. D. Stout

INTRODUCTION

THIS paper is one of a series resulting from a project at Lehigh University sponsored by the Fabrication Division of the Pressure Vessel Research Committee. This project has as its object, the study of the effect of various fabrication operations such as welding, cold forming and heat treatment on the mechanical properties of pressure-vessel steels.

One of the steps in fabrication is the sizing of plates by machining, flame cutting or shearing. The question arises whether these operations will affect the notch toughness of the steel by introducing notches or metallurgical changes. It may be said that these conditions will later be erased during the joining of the plate edges by welding; but not all of the edges necessarily receive welding, and others may be incompletely joined by "intermittent" welds.

It was decided to investigate the notch toughness of steel plate which had been prepared by machining, by flame cutting or by shearing.

TEST PROCEDURE

Specimen Design

In order to measure the effect of edge preparation on notch toughness, it was necessary to develop a specimen that would incorporate a notch in which the prepared edge would be an integral part. This was to avoid an artificial notch which would mask the effect of the original edge condition. The design which was adopted is shown in Fig. 1. This specimen is tested under slow bending, with the center notch in tension.

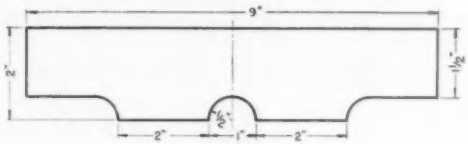

Fig. 1 Plate-edge effect test specimen

It will be noticed that the notch is of large radius to permit its preparation by shearing or flame cutting. The ends of the specimen are undercut to prevent possible tilting during bending. The width of the specimen is equal to the original plate thickness.

The Steels

The steels used for these tests were the two pedigreed steels described fully in previous reports.* They were A.S.T.M. grades A-201 and A-285 5/8 in. thick and analyzing as follows:

Steel Analysis

	C	*Mn*	*P*	*S*	*Si*	*Cr*	*Ni*	*Cu*
A-201	0.15	0.53	0.20	0.022	0.20	0.04	0.05	0.07
A-285	0.20	0.35	0.19	0.028	0.02	0.04	0.10	0.14

All specimens were cut from plates in the as-rolled condition.

Specimen Preparation

The significant step in the preparation of the specimens was the production of the notch contour. Three series of specimens were prepared: one by machining, one by flame cutting and one by shearing.

The machined notches were obtained by drilling and reaming a 1-in. diameter hole in the plate and then splitting the plate through the hole on a band saw. Two specimens could then be produced from the pieces.

The flame-cut notches were made on a special ma-

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* Osborn, C. J., Scotchbrook, A. F., Stout, R. D., and Johnston, B. G., "Composition and Property Variations of Two Steels," THE WELDING JOURNAL, 28 (5), Research Suppl., 227-s to 235-s (1949).

chine which provided a circular motion at the proper speed for a standard cutting torch.

The sheared notches were produced by punching a 1-in. diameter hole in the plate on a commercial press and then splitting the plate as was done for the drilled holes.

Enough specimens were prepared to permit postheating to be a part of the study. Postheating was carried out for 1 hr. at 500, 1150 and 1600° F. (the latter on A-201 steel only).

The appearance of the prepared notches is represented in Fig. 2.

Testing

The specimens were in sufficient number to allow tests over a range of temperature. The observations recorded during the tests included the percentage of lateral contraction at the bottom of the notch and the energy absorbed to failure. The transition temperature was defined as that at which the contraction or energy absorption had dropped to half its value in the shear fracture temperature range.

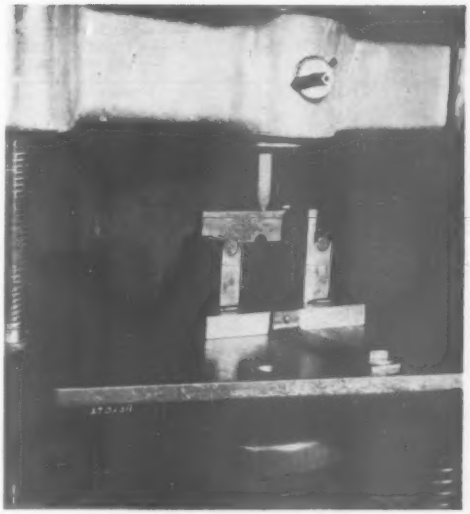
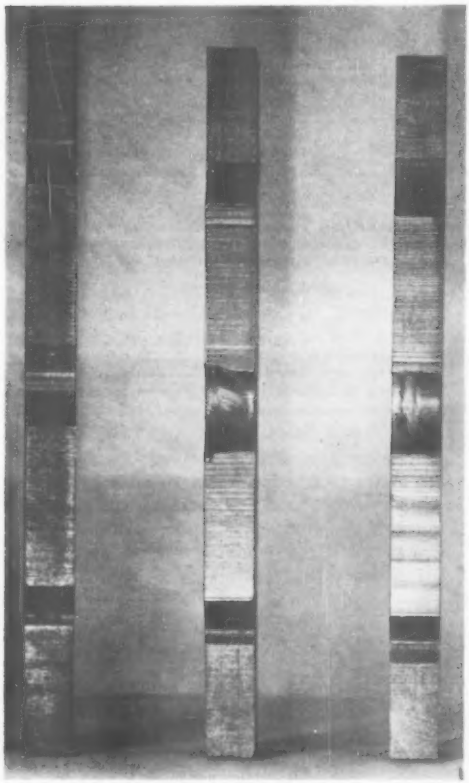


Figure 3

Testing temperatures were obtained by a cooling (or heating) bath from which the specimens were transferred to the testing jig shown in Fig. 3. Note the guide at one side of the jig to prevent tipping of the specimen. The crosshead of the tensile machine moved at the rate of 2.7 in. per minute during testing.

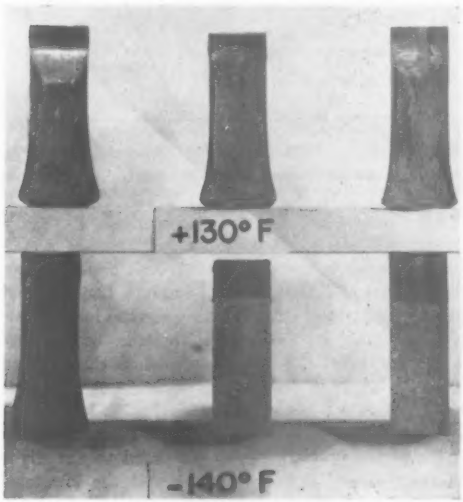
RESULTS AND DISCUSSION

The results of the investigation are presented as transition temperature curves in Figs. 5 through 11.



Machined Flame Cut Sheared

Figure 2



Machined Flame Cut Sheared

Figure 4

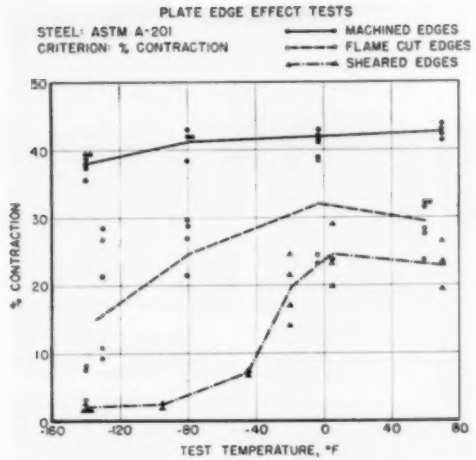


Fig. 5 Transition curves of A-201 steel, 5/8 in. thick, showing the effect of machined, flame-cut and sheared edges without heat treatment

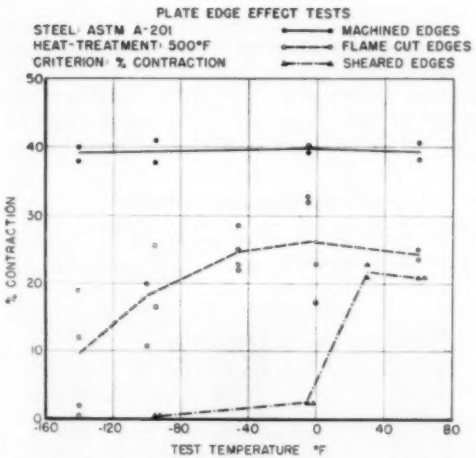


Fig. 7 Transition curves of A-201 steel, 5/8 in. thick, showing the effect of machined, flame-cut and sheared edges after heat treatment at 500° F.

The transition temperatures have been selected from these curves and plotted in a summary bar graph in Fig. 12 for easier examination. Only the lateral contraction data have been given, because the energy-absorption criterion leads to identical conclusions and virtually identical transition temperatures. Typical fractured specimens are shown in Fig. 4. Note the large deformation and shear fractures evident in tests at the higher temperature as compared to the slight deformation and cleavage fractures at the low temperature, except for the machined notch.

Examination of Fig. 12 reveals several points of interest. In both steels the machined notches were too

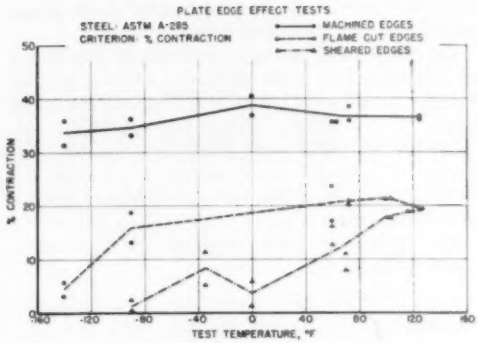


Fig. 6 Transition curves of A-285 steel, 5/8 in. thick, showing the effect of machined, flame-cut and sheared edges without heat treatment

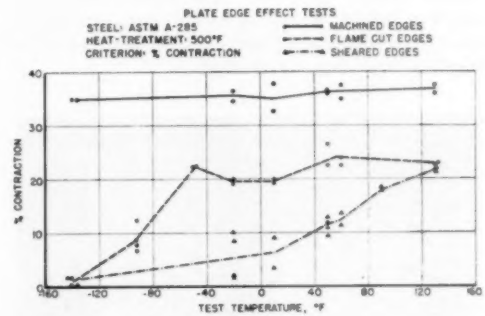


Fig. 8 Transition curves of A-285 steel, 5/8 in. thick, showing the effect of machined, flame-cut and sheared edges after heat treatment at 500° F.

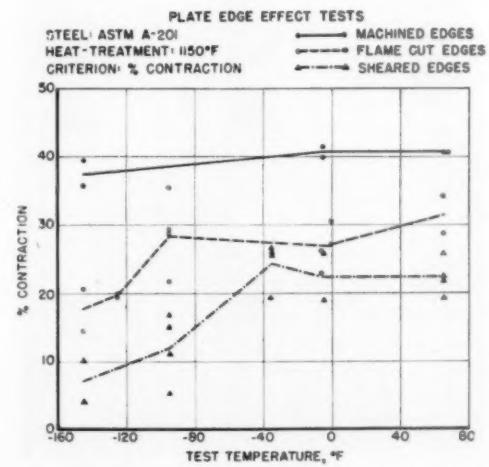


Fig. 9 Transition curves of A-201 steel, 5/8 in. thick, showing the effect of machined, flame-cut and sheared edges after heat treatment at 1150° F.

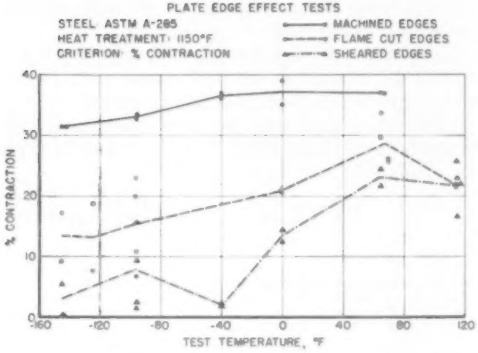


Fig. 10 Transition curves of A-285 steel, 5/8 in. thick, showing the effect of machined, flame-cut and sheared edges after heat treatment at 1150° F.

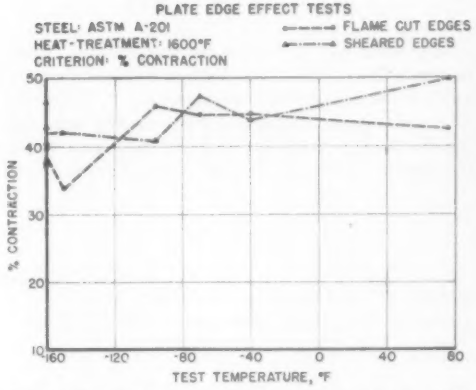


Fig. 11 Transition curves of A-201 steel, 5/8 in. thick, showing the effect of flame-cut and sheared edges after heat treatment at 1600° F.

mild to produce a transition even at −140° F., the lowest temperature tested. The flame-cut notches had higher transition temperatures, but they were not nearly so high as those obtained with the sheared notches.

Heat treatment at 500° F. had an adverse effect on both flame-cut and sheared specimens, whereas treatment at 1150° F. improved both types noticeably.

The postheating at 1600° F. of the A-201 plates was especially significant. The transition temperatures of both flame-cut and sheared specimens were lowered beyond −160° F. This is strong evidence that the harmful effects of these methods of edge preparation are not due to the sharp notches that are introduced by the ripples of the flame-cut or the small tears accompanying shearing, but rather that the heat effect or the plastic deformation is responsible. In other words, the adverse effects are not due to geometrical factors such as tiny notches; they are due to metallurgical changes resulting from flame cutting or shearing.

One other factor must not be overlooked with respect to the 1600° F. treatment. In the furnace, the atmosphere scales the steel and thus sloughs off a few thousandths of an inch of the metal. This can conceivably contribute to the improvement by its removal of superficial imperfections. It is not believed, however, that this effect was important in these tests.

It can therefore be concluded that flame cutting, and especially shearing, lower the notch toughness of the plate. In flame cutting, the rapid thermal cycle probably produces microstructures of reduced ductility. In shearing, the cut edge of the plate is so severely deformed plastically that it retains little ductility when further deformation is imposed. Since heat treatment at 1150° F. relieves these conditions somewhat, it is measurably beneficial. Normalizing at 1600° F. restores the as-rolled properties of the plate. Thus local flame normalizing of either of these edge conditions might be of definite benefit in actual fabrication.

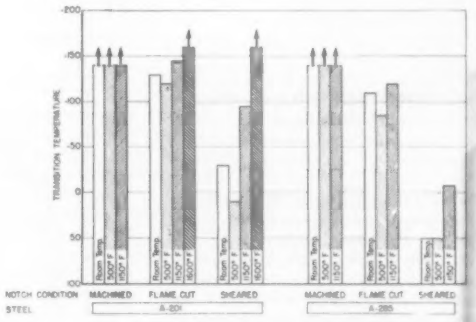


Fig. 12 Summary of transition temperatures as affected by edge preparation

SUMMARY

1. A specimen has been developed to allow study of the effect of plate-edge preparation on notch toughness.

2. Flame cutting and especially shearing lower the notch toughness.

3. Postheating at 500° F. raises further the transition temperature of flame-cut and sheared specimens. Postheating at 1150° F. is noticeably beneficial to the steels.

4. Postheating at 1600° F. lowers the transition temperature below −160° F. This behavior is strong evidence that the adverse effects of flame cutting or shearing are not due to the formation of notches at the surface but rather are due to metallurgical changes in the metal at the prepared edge by heating or plastic deformation.

5. The results of this investigation suggest that local flame-normalizing of flame-cut or sheared edges may be beneficial to the notch toughness of the steel plate.

ACKNOWLEDGMENTS

This work was sponsored by the Pressure Vessel Research Committee of the Welding Research Council, which is directed by William Spraragen. P. R. Cassidy is Chairman of the Pressure Vessel Research Committee and Boniface E. Rossi is Executive Secretary. F. L. Plummer is Chairman of the Fabrication Division of the Pressure Vessel Research Committee, which guided the project staff in this work.

The project was carried on jointly by the Fritz Engineering Laboratory of the Civil Engineering Dept., and the Metallurgy Dept. of Lehigh University.

The execution of work was made possible by the co-operation of Kenneth R. Harpel, laboratory foreman, and the entire laboratory staff.

Effects of Light Peening on the Yielding of Steel

Discussion by N. H. Polakowski

I wish to congratulate the authors for their simple and ingenious approach to a problem which is practically important but inherently complex at the same time. It may perhaps be of interest to reproduce here some of my own results which not only bear out the observations made by Professor Mills and Mr. Harrison but, I believe, also explain their reasons.

In the course of an investigation of the discontinuous yield phenomenon, steel rods 0.25 in. in diameter and 10 in. long were hammered by hand with a 4-oz. wooden mallet for different time intervals. The blows were divided as uniformly as possible over the whole length of each sample and afterwards the rods were tested in tension by using a plain 4-in. extensometer attached to an autographic recording device. A set of records obtained with a 0.14% C, commercial-quality steel, normalized from 900° C. is shown in Fig. 1.

It is seen that the initial yield stress (or load) values decrease systematically with increasing intensity of the hammering treatment applied, as indicated by the small arrows. Since the actual yield stress is depressed by hammering, it is only natural that large permanent sets are found at relatively low stresses. In fact, yielding begins at much lower stress values than those indicated on the consecutive curves in Fig. 1, because a steel subjected to hammering and not allowed to age has probably no elastic range at all, just like common nonferrous metals. This could not be detected in my experiments because of the crude extensometer used, but it is a well-known fact since Bauschinger's time.

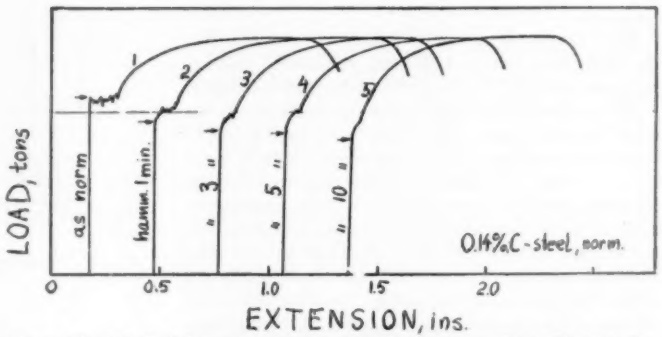


Fig. 1 The effect of hammering for periods indicated on the yield-point behavior of normalized, 0.14% C-steel rods, 0.25 in. in diameter, and 10 in. long. Gage length 4 in. in all tests

The fall of the yield stress and the simultaneous disappearance of the yield discontinuity can be accounted for if it is assumed that discontinuous yielding of steel is caused by a brittle "grain-boundary skeleton," as already suggested by numerous prominent investigators in the past. The above "skeleton" consists most likely of cementite, some nitrides and perhaps of other inclusions as well. Hammering destroys the brittle structure embedded in the soft ferrite without seriously affecting the ductility of the matrix, which has a much lower inherent yield stress value than the "composite body," i.e., the annealed but mechanically unworked steel.

The above concept is consistent with the authors' observation that the elongation was confined to that portion of the test-piece which was actually hammered (p. 253, top, left), because only in this portion was the yield point depressed.

A reference to their Fig. 3 suggests that some small stretching was caused by the action of the hammer alone (lowest curve), and this would indicate that some work-hardening must have occurred.

I would be glad to know whether the changes of hardness caused by different numbers of blows are available to the authors (Vickers or Brinell values preferably), as some increase could be expected in spite of the simultaneous depression of the yield point.

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Paper by H. L. Harrison and B. D. Mills, Jr., was published in the Research Supplement to the May 1951 WELDING JOURNAL, p. 251.

Effect of Cooling Rate on the Aging of Structural Steels

Strain aging of structural steels is enhanced by rapid cooling from 1200° F. as by mass quenching near-weld zones

by C. R. Felmley, C. E. Hartbower and W. S. Pellini

Abstract

The quench and strain-aging characteristics of structural steels has been shown to be dependent on the rate of cooling from 1200° F. The steels investigated included rimmed, silicon-killed, aluminum killed and vanadium-titanium killed types. Rapid cooling as obtained by quenching from 1200° F. enhances aging propensities regardless of steel deoxidation practice. The fully killed types develop strong aging characteristics which are otherwise absent in slow-cooled material. The development of aging effects in near-weld zones as the result of mass quenching has been demonstrated by strain-aging tests of near-weld zones for welds representative of various cooling rates.

THE RELATION OF AGING TO THE PERFORMANCE OF WELDED JOINTS

LOADING of complex, continuous-frame welded structures results in localized plastic flow at points of stress concentration. Such flow at isolated joints, which may be termed "plastic hinges," causes a general redistribution of loads throughout the assembly; this behavior is referred to in the field of mechanics as redundancy or in more common terms as "shakedown." The various members of the structure thereby assume loading conditions aimed at by the designer. Inasmuch as welds are generally located at points of discontinuity associated with plastic-hinge action, the weld zone is often strained into the plastic region at loads for which the remainder of the structure is behaving elastically. The effect of aging reactions on the ductility and flow characteristics of weld zones thus are important considerations to the designer of welded structures.

There are several basic aspects of the weld-zone aging problem which should be recognized:

1. Quench aging which may occur without the requirement of previous plastic flow; i.e., prior to the shakedown of a structure.
2. Strain aging which may occur as the result of shakedown, i.e., during or after plastic flow resulting from loading.
3. The possible algebraic additivity or interaction of quench and strain aging; i.e., the possibility of quench aging affecting the development of strain aging and vice versa.
4. The contribution of steel quality such as result from variations of deoxidation practice.
5. The effect of the weld thermal cycle such as rapid or slow cooling from elevated temperature.

The weld-zone aging problem is an exceedingly complicated one which requires careful study from a standpoint of both the weld bead and the heat-affected zone. Actually, relatively little has been done to isolate the aging propensities of the individual elements of a weldment. As a first approach to the question of the heat-affected zone it is necessary to have a considerably better basic understanding of the effect of simple variations of cooling conditions on the aging characteristics of the various grades of steel than is presently available. The contribution of complex weld cooling conditions may then be approached with greater assurance of correct interpretation of the effects noted. The main portion of this report presents data obtained by such a simplified first approach. The significance of these data is in turn evaluated by critical experiments concerned with weld zones.

METALLURGICAL CONSIDERATIONS

Aging of steels is a process which represents a trend toward constitutional equilibrium away from metastable solid solution. The process is characterized by increased hardness and tensile flow strength, loss of ductility, changes of magnetic properties, etc. "Quench aging" has been used to define the occurrence of such changes following rapid cooling from solution temperatures while "strain aging" has been applied to similar changes following mechanical treatments producing plastic flow.

The exact nature of the mechanism which produces aging effects has not been established conclusively. Considerable evidence points to precipitation reactions in keeping with the established age-hardening hypotheses. According to these hypotheses, elements which have high solubility in ferrite at elevated temperatures and low solubility at normal temperatures may give rise to aging precipitation provided supersaturation conditions are developed. This is accomplished by taking the subject elements into solution at the elevated temperature and cooling sufficiently rapidly to result in enforced solution at normal temperatures. Increasing the cooling rate increases the degree of supersaturation and, accordingly, the metastability of the resultant solid solution.

Carbon and nitrogen[1] have been shown to be primarily responsible for aging in mild steel. The presence of as little as 0.001% of these elements in solid solution is sufficient to produce the effect. It is thus apparent that extremely strong carbon and nitrogen fixers or scavengers must be employed to produce steels of low aging susceptibility. The "nonaging" class of commercial steels are nitrogen-fixed by additions of strong carbide formers such as vanadium and titanium. Titanium serves a dual purpose in this respect.

A preponderance of the basic information relative to the kinetics of aging reactions, additivity of quench and strain aging effects, overaging characteristics, etc., is based on hardness tests conducted on steels containing 0.03 to 0.05% C.

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Davenport and Bain[3] point out that the most pronounced effects are noted for material containing 0.04% C which they report is not sufficient to fully saturate the ferrite at optimum solution temperatures. Inasmuch as hardness changes due to aging reactions are not pronounced for steels above 0.15% C it is difficult to apply the findings based on the very low-carbon steels to the case of commercial mild steels. A considerable confusion has developed relative to literature cross-referencing of aging effects determined by hardness changes on the low-carbon material with tensile and impact tests conducted on commercial steels. In many cases apparent lack of agreement may be reconciled by consideration of the test materials employed.

The development of embrittlement consequent to strain aging appears to be generally accepted in keeping with the preponderance of experimental data in this respect.[3] The case for quench-aging embrittlement, however, appears to be less definitely established. The 1948 *Metals Handbook*, for example,[4] summarizes the extent of available knowledge as indicating that "quench aging lowers the impact resistance but seldom so much as to result in actual brittleness." This is a most important point for if true and if quench aging is subtractive to strain aging, it would indicate a means of guiding the precipitation to an inocuous form. Further analysis of this point with respect to the authors' findings and the significance to welding problems will be made as part of a general discussion of this report.

Harrison's[5] investigation of the embrittling effects of quench and strain aging is the only report known to the authors which considers the several aspects of additivity of aging reactions, relative embrittling effects of the two reactions and effect of enforced solution occasioned by quenching. Unfortunately, the coverage of these aspects is relatively sketchy. For steels of 0.20% C levels it is indicated that quenching promotes both quench- and strain-aging embrittlement as compared to slow-cooled material. Over-quench-aging effects are not indicated even with holding times of two months in contradiction to overaging in periods of less than 1 month reported[2] for the low-carbon (0.06% C) ferrites.

It may be summarized that relatively little is known about the possible interaction of quench and strain aging in mild steel. In general a steel which shows quench aging will also show strain aging; the reverse, however, is not always true. The question of which type of aging is most detrimental likewise has not been satisfactorily answered. The effect of cooling rate from subcritical temperatures on the development of supersaturation and consequent intensification of aging effect appears to have been generally accepted but quantitative data as to the relative effect on the various types of structural steels are lacking.

The condition of enforced solution created by a subcritical heating followed by rapid cooling may be expected when welding is involved. Flanigan[6] reports that strain aging appears to be characteristic of arc welds in mild steel regardless of the previous steel deoxidation practice or type of electrode employed. His experimental results show an apparent interrelation between quench and strain aging; i.e., increasing the quench-aging time at room temperature prior to prestraining minimized subsequent strain-aging effect. He concludes that strain aging of weldments occurs in the weld metal rather than in the heat-affected zone or parent plate. On the other hand, Fountain and Stout[7] show from the results of bead-on-plate notch-bend tests on semikilled steel, that strain aging occurs only in the heat-affected zone of the base metal.

From metallurgical considerations it is expected that the aging of near-weld zones should depend on the combined effects of cooling rate and deoxidation practice of the steel. Actually, very little quantitative data separating the effects are available and it appears desirable that the heat-affected zone should be more thoroughly investigated for the various typical grades of structural steels.

PLAN OF INVESTIGATION

The general aim of the investigation was the evaluation of the effects of cooling rate from high subcritical temperatures on the development of strain and quench aging and the determination of the embrittlement which followed. Two cooling rate extremes were investigated: (1) slow cooling such as is obtained in normalizing or annealing operations and (2) quench cooling such as may be obtained in a near-weld zone. These conditions may be taken as indicative of the cooling histories of structural steel unaffected by the heat of welding and of the material in the near-weld zone. It is recognized that the actual cooling conditions of near-weld zones vary widely depending on size of weldment and welding techniques. Slow- and quench-cooling conditions were developed by suitable treatments on finish-machined 0.505 tensile and Charpy specimens.

Four commercial structural steels were investigated: rimmed, Si killed, Al killed and Ti and V killed, thus encompassing the range of common deoxidation practices. Chemical analyses and tensile properties are listed in Table 1.

In view of the difficulties inherent to interpreting hardness changes, tensile and notch-bend tests were used throughout this investigation. The tensile-test results were taken only as indicative of the development of aging reactions; no especial significance was attached to in-

Table 1—Chemical Analyses and Tensile Properties

	Material	*C*	*Mn*	*Si*	*S*	*P*	*Ni*	*Cu*	*Cr*	*Al*	*Ti*	*V*	*Mo*	*Acid soluble N_2, 1-1HCl*	*Acid insoluble N_2*
1.	Ti-V killed 1-in. plate	0.15	1.06	0.22	0.022	0.012	0.08	0.03	0.07	0.018	0.022	0.06	...	0.006	0.003
2.	Al-killed 1 1/2-in. plate	0.20	0.58	0.21	0.027	0.018	0.074	0.13	0.047	0.04	...	...	0.007	0.006	nil
3.	Silicon-killed 1-in. plate	0.23	0.54	0.12	0.041	0.012	0.048	0.044	0.055	0.01	...	...	0.005	0.005	nil
4.	Rimmed-steel 3/4-in. plate	0.19	0.35	0.01	0.028	0.018	0.14	0.33	0.065	0.01	...	...	0.029	0.007	0.003

Tensile properties (normalized at 1650° F. for 1 1/2 hr.)

Material	*Yield strength at 0.2% offset, psi.*	*Tensile strength, psi.*	*Elongation, in 2 in., %*	*Reduction, in area, %*
Ti-V killed steel	48,100	68,400	39.5	73.9
Al-killed steel	42,100	62,200	39.5	67.2
Silicon-killed steel	39,600	64,000	38.0	63.8
Rimmed steel	41,200	64,000	36.5	58.0

creases in flow resistance occasioned by the aging reactions in terms of strength properties. The effect of the aging reactions on ductility was evaluated by notch-bend tests since such are more discriminating and indicative of embrittling effects than the terminal ductility values obtainable from the tensile specimens. The use of small notched tensile specimens (to be described) provided micro-scale tensile tests of the very small volume of metal of near-weld zones. The significance of the data obtained by synthetic cooling cycles applied to conventional specimens was thus evaluated.

To simplify discussion and tables the following terms and definitions are used to describe the various heating, cooling and aging treatments.

Coded	*Treatment*
Normalize	Initial treatment (1650° F., 1½ hr., air-cooled). Material in the form of bars 2 x 10 in. x plate thickness.
Quench cool	Finish-machined specimens solution heat-treated at 1200° F. for 6 hr. in an evacuated quartz tube followed by brine quenching.
Furnace cool	Finish-machined specimens solution heat-treated 1200° F. for 6 hr. followed by furnace cooling in an evacuated quartz tube at approximately 60° F. per hour.
Minimum quench aging	Aging at +70° F. for the minimum time required to complete testing: 1 hr. for tensile tests; 1 day for Charpy tests.
Prolonged quench aging	Aging at +70° F. for 30 days.
Accelerated strain aging	Aging at 200° F. for 16 hr. after prestraining.
R.T. strain aging	Aging at room temperature (+70° F. for 30 days) after prestraining.
Prestrain	Controlled straining to a definite limit.

Autographic load-elongation curves for the standard tensile specimens were obtained by means of a microformer recorder operated at a magnification of 20 ×. The development of strain aging was evaluated by the increase in reloading yield point following the aging treatment. Quench aging on the other hand was evaluated by the increase in the height of the flow curve following the quench-aging treatments. Figure 1 presents a detailed listing of the processing of the various groups of tensile specimens.

The Charpy keyhole specimens followed the processing indicated by the flow chart of Fig. 2. The prestraining operation for these specimens was performed in a small impact tester by a 50-in.-lb. blow. A reproducible, limited amount of deformation (angle of bend approximately 5°; lateral contraction at notch approximately 2.0%) was obtained as a result of the prestraining operation; no cracking was apparent at the root of the notch. A group of specimens was tested for each test series immediately after prestraining. The transition curves for these specimens were then compared with companion groups of strain-aged specimens. A minimum of 15 specimens were employed to determine each transition curve.

Tensile tests of near-weld-zone areas were conducted by means of the micro-tensile specimen shown in Fig. 6. The load-elongation curves for prestraining and final testing of these specimens were determined by a microformer recorder operated at a magnification of 50 ×. The processing of these specimens is described in the weld-test section of the report.

DEVELOPMENT OF AGING AS INDICATED BY TENSILE TESTS

Effect of Strain Level on Subsequent Strain Aging

As a preliminary step in the tensile investigation, 0.505 tensile bars of all four steels in the normalized conditions were prestrained to levels of 0.05, 0.10 and 0.15 in./in. offset followed by the accelerated strain-aging treatment. This series was aimed at deducing the effect of prestrain level on the development of the strain aging. The step-by-step treatment of these specimens is shown as Group 1 in Fig. 1. The individual load-elongation curves for the four different steels are shown in Fig. 3.

Varying the amount of prestrain appeared to produce a uniform rise in the resulting flow curve for each of the steels as shown by the dotted line connecting the reloading yield points (Fig. 3). It appears that increasing the level of strain in the range of uniform deformation (between yield and neck) does not increase the amount of strain aging as indicated by the rise in the reloading yield point. Evidently a certain strain-aging potential is made fully operative by a small strain and is not increased by further straining. Accordingly, all subsequent prestraining operations were arbitrarily limited to the 0.10 in./in. level.

This series also shows that in the normalized condition the two fully killed steels (Ti-V and the Al killed) are only slightly susceptible to strain aging as compared to the silicon killed and the rimmed steels.

Effect of Slow Cooling on Aging

The detailed handling of the two groups of specimens which were furnace-cooled at the rate of 60° F. per hour from 1200° F. is shown in Fig. 1. The load-vs.-elongation curves for the group which was tested without delay are shown in Fig. 4 (A) as Group 2 (solid curve). The slow cooling rate employed was sufficient to eliminate the small amount of strain aging observed in normalized Ti-V and Al killed steels. The amount of strain aging obtained for the Si killed and rimmed steels remained very pronounced.

A companion group of furnace-cooled specimens was stored for 30 days at +70° F. prior to straining and aging at room temperature. The processing of these specimens is shown as Group 3 in Fig. 1. The load-vs.-elongation curves of the specimens in Fig. 4 (A) Group 3, is represented by the dashed line superimposed on the Group 2 series for compar-

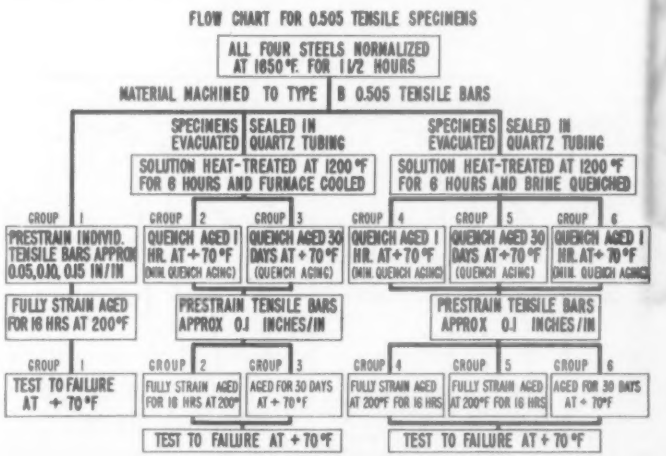
FLOW CHART FOR 0.505 TENSILE SPECIMENS
ALL FOUR STEELS NORMALIZED AT 1650°F. FOR 1 1/2 HOURS
MATERIAL MACHINED TO TYPE B 0.505 TENSILE BARS
SPECIMENS EVACUATED
SEALED IN QUARTZ TUBING
SOLUTION HEAT-TREATED AT 1200°F FOR 6 HOURS AND FURNACE COOLED
SPECIMENS EVACUATED
SEALED IN QUARTZ TUBING
SOLUTION HEAT-TREATED AT 1200°F FOR 6 HOURS AND BRINE QUENCHED
GROUP 1
PRESTRAIN INDIVID. TENSILE BARS APPROX 0.05, 0.10, 0.15 IN/IN
GROUP 2
QUENCH AGED 1 HR. AT +70°F (MIN. QUENCH AGING)
GROUP 3
QUENCH AGED 30 DAYS AT +70°F (QUENCH AGING)
GROUP 4
QUENCH AGED 1 HR. AT +70°F (MIN. QUENCH AGING)
GROUP 5
QUENCH AGED 30 DAYS AT +70°F (QUENCH AGING)
GROUP 6
QUENCH AGED 1 HR. AT +70°F (MIN. QUENCH AGING)
FULLY STRAIN AGED FOR 16 HRS AT 200°F
PRESTRAIN TENSILE BARS APPROX 0.1 INCHES/IN
PRESTRAIN TENSILE BARS APPROX 0.1 INCHES/IN
GROUP 1
TEST TO FAILURE AT +70°F
GROUP 2
FULLY STRAIN AGED FOR 16 HRS AT 200°F
GROUP 3
AGED FOR 30 DAYS AT +70°F
GROUP 4
FULLY STRAIN AGED AT 200°F FOR 16 HRS
GROUP 5
FULLY STRAIN AGED AT 200°F FOR 16 HRS
GROUP 6
AGED FOR 30 DAYS AT +70°F
TEST TO FAILURE AT +70°F
TEST TO FAILURE AT +70°F

Figure 1

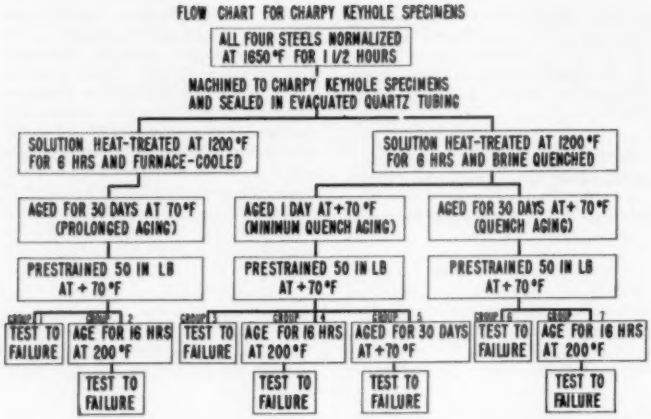
FLOW CHART FOR CHARPY KEYHOLE SPECIMENS
ALL FOUR STEELS NORMALIZED AT 1650 °F FOR 1 1/2 HOURS
MACHINED TO CHARPY KEYHOLE SPECIMENS AND SEALED IN EVACUATED QUARTZ TUBING
SOLUTION HEAT-TREATED AT 1200 °F FOR 6 HRS AND FURNACE-COOLED
SOLUTION HEAT-TREATED AT 1200 °F FOR 6 HRS AND BRINE QUENCHED
AGED FOR 30 DAYS AT 70 °F (PROLONGED AGING)
AGED 1 DAY AT + 70 °F (MINIMUM QUENCH AGING)
AGED FOR 30 DAYS AT + 70 °F (QUENCH AGING)
PRESTRAINED 50 IN LB AT + 70 °F
PRESTRAINED 50 IN LB AT + 70 °F
PRESTRAINED 50 IN LB AT + 70 °F
GROUP 1
TEST TO FAILURE
GROUP 2
AGE FOR 16 HRS AT 200 °F
TEST TO FAILURE
GROUP 3
TEST TO FAILURE
GROUP 4
AGE FOR 16 HRS AT 200 °F
TEST TO FAILURE
GROUP 5
AGED FOR 30 DAYS AT + 70 °F
TEST TO FAILURE
GROUP 6
TEST TO FAILURE
GROUP 7
AGE FOR 16 HRS AT 200 °F
TEST TO FAILURE

Figure 2

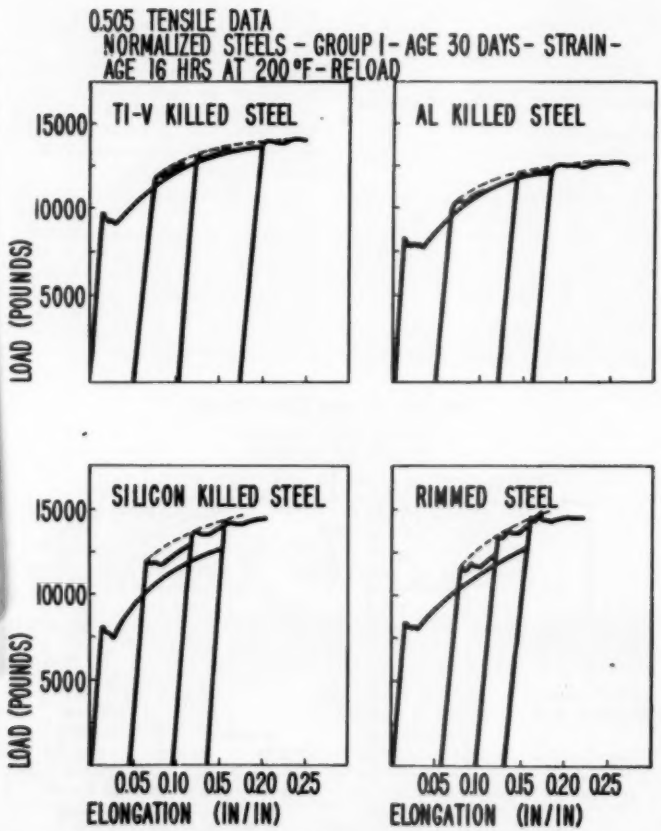

Fig. 3 Relation of the degree of prestrain to subsequent strain aging as indicated by reloading yield point

ative purposes. Holding at room temperature for this extended period of time prior to prestraining did not produce a significant amount of quench aging. It is also evident that room temperature holding following the furnace-cooling treatment did not alter the strain-aging potential for any of the steels. Consequently it may be concluded that the four steels were not susceptible to quench aging after the furnace-cooling operation and that the amounts of strain aging obtained were independent of room-temperature holding time.

Effect of Quench Cooling on Aging

Tensile specimens machined from normalized material were given a solution heat treatment at 1200° F. for 6 hr. followed by brine quenching to promote a highly metastable solution condition. Three specimen groups were treated in this manner prior to the subsequent quench- and strain-aging operations. These included:

1. A group which was allowed a minimum of R.T. quench-aging time prior to straining and aging at 200° F.
2. A group similar to item 1 except that strain aging was developed by prolonged holding at R.T. following straining.
3. A group which was allowed a prolonged time at R.T. to develop quench aging prior to straining and aging at 200° F.

The aim of this specific study was to deduce the effect of length of holding time following the quench operation on the development of quench and strain aging, also, the effect and/or equivalence of accelerated strain aging as compared to prolonged R.T. strain aging.

Figure 1, Groups 4 and 6, presents the processing details of the minimum quench-aging series (items 1 and 2 above). Tensile data are presented in Fig. 4 (*B*). The prestraining portion of the load-elongation curves for the furnace-cooled specimens (which did not develop quench aging) is also shown to serve as a base line for estimation of quench-aging effects. It is observed that pronounced and similar amounts of quench aging developed for all steels in the 1-hr. time interval between quenching and straining. This is deduced from the rise-in-flow curve above the level shown by the furnace-cooled steels as denoted by the dashed area. It is also shown that pronounced and similar amounts of strain aging are developed in all steels. Moreover, the effect of accelerated aging (200° F. 16 hr.) is not significantly different from that of the prolonged R.T. aging (70° F. for 30 days). This may be taken to indicate that the inherent strain-aging potential is fully developed by either treatment.

The processing details of the group which was allowed prolonged quench aging

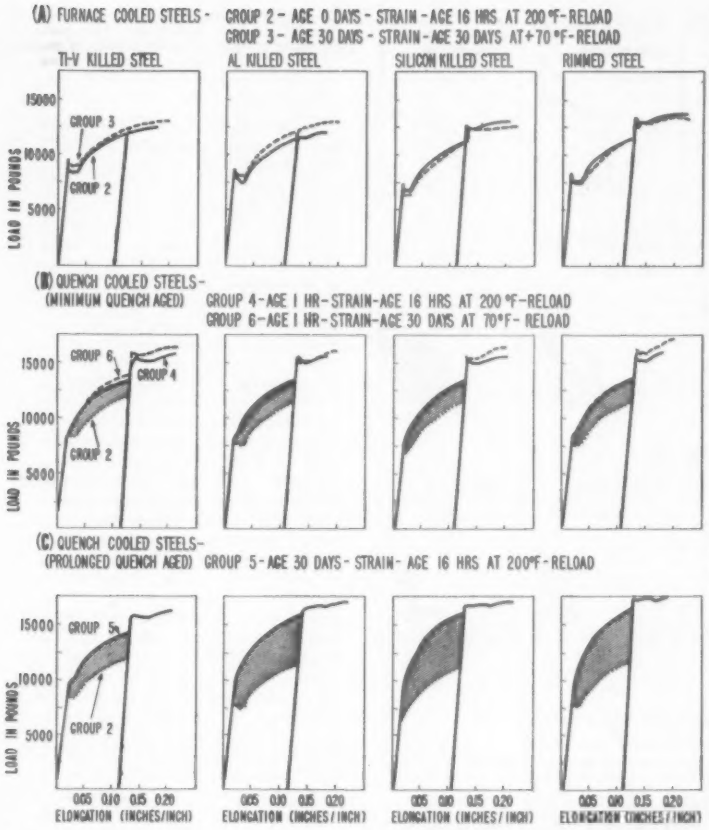
(A) FURNACE COOLED STEELS - GROUP 2 - AGE 0 DAYS - STRAIN - AGE 16 HRS AT 200 °F - RELOAD
GROUP 3 - AGE 30 DAYS - STRAIN - AGE 30 DAYS AT + 70 °F - RELOAD
TI-V KILLED STEEL
AL KILLED STEEL
SILICON KILLED STEEL
RIMMED STEEL
LOAD IN POUNDS
15000
10000
5000
GROUP 3
GROUP 2
(B) QUENCH COOLED STEELS - (MINIMUM QUENCH AGED) GROUP 4 - AGE 1 HR - STRAIN - AGE 16 HRS AT 200 °F - RELOAD
GROUP 6 - AGE 1 HR - STRAIN - AGE 30 DAYS AT 70 °F - RELOAD
GROUP 6
GROUP 4
GROUP 2
(C) QUENCH COOLED STEELS - (PROLONGED QUENCH AGED) GROUP 5 - AGE 30 DAYS - STRAIN - AGE 16 HRS AT 200 °F - RELOAD
GROUP 5
GROUP 2
0.05 0.10 0.15 0.20
ELONGATION (INCHES/INCH)

Fig. 4 Summary of 0.505 in tensile-test data indicating development of quench and strain aging

(item 3 above) are given in Fig. 1, Group 5. Tensile data are presented in Fig. 4 (*C*). It is evident from observation of the dashed area indicating the extent of the rise-of-flow curve above the furnace-cooled steel base line, that a decided increase in quench aging is developed by increasing the holding time to 30 days following quenching. The effect is pronounced in all steels except the Ti-V killed steels for which the increase is only moderate. It is observed also by comparison with Fig. 4 (*B*) that strain aging is markedly decreased in all of the steels which showed a marked increase in quench aging. This indicates that the development of quench aging is subtractive to strain aging which may follow at least in a qualitative sense.

Effect of Aging Reactions on Notch Ductility

The extent to which the various aging reactions indicated by the tensile tests influenced notch ductility was determined by means of Charpy keyhole transition curves. The heat-treatment and straining programs of the various series of Charpy specimens paralleled those employed for the tensile phase of the investigation to permit direct intercomparisons. A summary of the processing details is provided in Fig. 2 and the results are plotted in Fig. 5. While it is not possible to make quantitative comparisons between aging indications of tensile specimens and Charpy notch-bend specimens, it will be shown that good qualitative correspondence exists. The following discussion is aimed at illustrating the correspondence which was developed; also, at summarizing the results of the tensile and notch-bend tests.

1. Tensile data indicated that slow-cooled steels developed pronounced strain-aging tendencies only in the case of the Si killed and rimmed varieties, Fig. 4 (*A*). It will be observed from Fig. 5 (*A*) that a marked shift in the transition curves to higher temperatures, denoting loss of notch ductility, was observed only for these two stated varieties. The effect is most pronounced for the rimmed steel; i.e., the tensile data failed to show a significant difference between the Si killed and the rimmed steels.

2. Tensile data indicated that all quench-cooled steels developed marked and similar quench aging within 1 hr. following the quench treatment, Fig. 4 (*B*). Also that increasing the holding time to 30 days resulted in a pronounced increase of quench aging with the exception of the Ti-V killed steel for which the increase was only moderate, Fig. 4 (*C*). The base line for evaluating quench-aging effects on transition temperature must as in the case of the tensile data again be taken as the basic curve for the furnace-cooled steels (Group 1 curve, Fig. 5 (*A*)). Comparison of the quench-cooled-steel curves of Figs. 5 (*B*) and 5 (*C*) with the stated base-line curve shows a small but consistent shift (compare 5 (*B*), Group 3 with 5 (*A*), group 1) of 20 °F. to higher temperature for all of the steels, indicating similar embrittling effect of the short-time (minimum) quench aging developed in these steels. Thus, as predicted by the tensile data all the steels behaved essentially alike. The effect of increasing the holding time to 30 days may be observed by comparing the Fig. 5 (*C*), Group 6, curve with Fig. 5 (*B*), Group 3, curve. A marked notch-ductility decrease due to the added quench aging is shown by the Si killed and rimmed steels while no significant difference is indicated for the Ti-V killed steel, which is in good agreement with the tensile data predictions. The Al-killed steel, however, shows only a moderate loss of notch ductility rather than a marked loss predicted by the tensile data.

3. Tensile data indicated pronounced strain aging for all of the steels following quench treatment and a minimum period of holding prior to straining and aging, Fig. 4 (*B*); moreover, that accelerated and R.T. strain-aging treatments were closely equivalent. A marked embrittlement for all steels due to the strain-aging treatment is shown in Fig. 5 (*B*); compare the Group 3 curve with the Group 4 and Group 5 curves. Except possibly for the rimmed and Si-killed steels it is evident that equivalent embrittlement effects are developed by accelerated and R.T. strain-aging treatments. Thus, a close correspondence of the tensile and notch data is again indicated.

4. Increasing the holding time of quench-cooled steels which results in increased quench aging was shown to decrease the consequent strain aging by the tensile-test data in the case of the Al-killed, Si-killed and rimmed steels. The same effect may be noted in terms of added embrittlement (added to the quench-aging embrittlement) shown in Figs. 5 (*B*) and 5 (*C*). In the case of the minimum quench-aged, Si-killed and rimmed-steel specimens the added embrittlement due

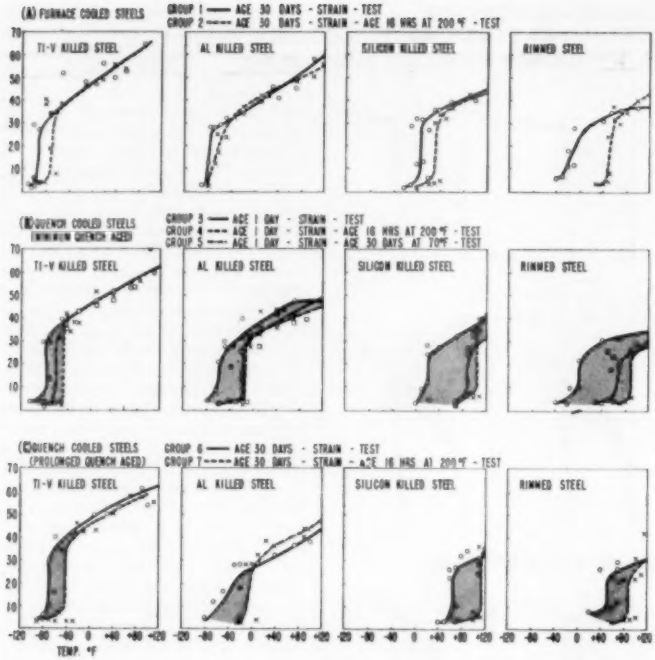
(A) FURNACE COOLED STEELS
GROUP 1 —— AGE 30 DAYS - STRAIN - TEST
GROUP 2 ---- AGE 30 DAYS - STRAIN - AGE 16 HRS AT 200°F - TEST
TI-V KILLED STEEL
AL KILLED STEEL
SILICON KILLED STEEL
RIMMED STEEL
(B) QUENCH COOLED STEELS (MINIMUM QUENCH AGED)
GROUP 3 —— AGE 1 DAY - STRAIN - TEST
GROUP 4 ---- AGE 1 DAY - STRAIN - AGE 16 HRS AT 200°F - TEST
GROUP 5 ---- AGE 1 DAY - STRAIN - AGE 30 DAYS AT 70°F - TEST
(C) QUENCH COOLED STEELS (PROLONGED QUENCH AGED)
GROUP 6 —— AGE 30 DAYS - STRAIN - TEST
GROUP 7 ---- AGE 30 DAYS - STRAIN - AGE 16 HRS AT 200°F - TEST
TEMP. °F

Fig. 5 Summary of Charpy keyhole transition data indicating the embrittling effects of quench and strain aging

to strain aging was considerably greater than the added strain-aging embrittlement developed in the 30-day quench-aged specimens. This is brought out more clearly by observing the dashed areas of Figs. 5 (*B*) and 5 (*C*), which denote the transition-curve shifts due to strain aging.

It is further observed that the position of the transition curves (Figs. 5 (*B*) and 5 (*C*)) representing the cumulative embrittling effects of quench aging plus strain aging is the same regardless of the amount of quench aging which preceded the strain-aging operation. There is, accordingly, an indication that a certain aging embrittling potential is developed by quench cooling which is made operative by either type of aging reaction. The path taken whether to extensive quench aging plus the small remanence as strain aging or minor quench aging plus a strong remanence of strain aging is apparently unimportant to the total amount of embrittlement finally developed. It should be noted that the four final curves of Figs. 5 (*B*) and 5 (*C*), groups 4 and 7, may be superimposed within limits of significance of test results.

The strong effect of quench cooling on the extent of embrittlement which may be developed in the various steels may be deduced by comparison of the cumulative curves for the quench-cooled and furnace-cooled materials. The shift in the transition curves occasioned by rapid cooling is roughly 40° F. for all steels except the silicon-killed steels for which it is 60° F.

DEVELOPMENT OF STRAIN AGING IN WELD ZONES

The marked effect of cooling rate from high subcritical temperatures on the strain-aging propensities of the various structural steels is of considerable importance because of the brittleness effects which accompany the aging reactions. The data developed by the tensile and Charpy tests described in previous sections should be of serious concern to the welding field if applicable to near-weld zones which attain high subcritical temperatures followed by rapid cooling developed by mass quench effects. Such brittle zones may act as triggers or intensifiers for cracks originating at weld sites.

A preliminary investigation was made to determine the applicability of the data. The extent of the investigations was purposely restricted to critical experiments because of time limitations. Accordingly,

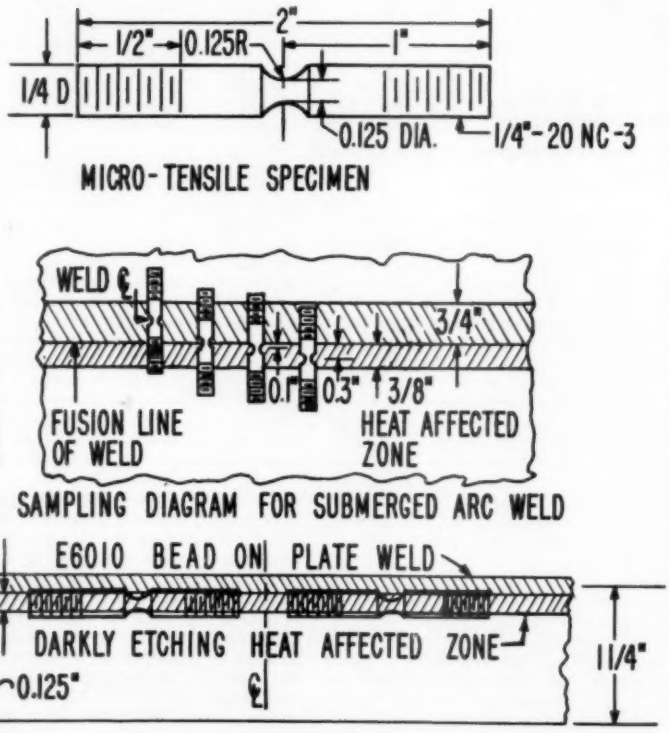

Fig. 6 Sampling diagram for near-weld zone specimens of bead-on-plate welds

the development of aging susceptibility was evaluated only on a basis of strain-aging effects. Continued investigations are under way to explore more fully this complex problem. The experiments were specifically aimed at resolving the following major points:

1. Are the very short times at subcritical temperature (1200° F. range), which are characteristic of weld thermal cycles, sufficient for solution of the aging elements?

2. Are the cooling rates obtained by conventional welding procedures sufficient to develop intensified aging effects?

Considerable difficulty is associated with conducting and interpreting notch tests of sections cut from weldments containing only small volumes of material of direct interest; i.e., a specific element of heat-affected zone for which the information is desired.

Accordingly, it was decided to test various positions in the HAZ separately by the use of suitable micro-scale specimens. Such a specimen was available in the form of the notched micro-tensile specimen shown in Fig. 6. This specimen had been used previously by the authors for determination of stress-strain curves of micro elements in the weld and HAZ of explosion bulge weldments.[8] The presence of potential aging conditions at various positions in the HAZ was determined by prestraining, aging and noting the development of an increased reloading yield point; viz., exactly in the fashion used for the 0.505 tensile-test investigation.

Preliminary to conducting the weld tests, micro-tensile specimens were machined from the furnace-cooled (stabilized with respect to aging) material and immersed in a lead bath at 1200° F. for times of 2, 3 and 5 secs. followed by brine quenching to room temperature. The cooling rate obtained was measured to be approximately 170° F. per second. After quenching the specimens were prestrained, aged at 200° F. for 16 hr. and reloaded. The results of these tests as shown in Fig. 7 (*A*) indicate that solution is obtained in the order of 3 secs. The specimen quenched after a 2-sec. immersion time only reached 950° F. before it was quenched. It would appear therefore that the solution time is very short and easily within the times obtained in weld thermal cycles.

Weld-aging tests were made utilizing a heavy, slow-cooling submerged arc weld and a fast-cooling bead-on-plate E6010 weld. The aluminum-killed steel was used for these tests since it was possible to completely eliminate aging tendencies in the base plate by furnace cooling prior to welding. Thus, any aging effects found would be directly ascribed to the weld thermal cycle. Tests were also conducted on normalized material which was mildly aging susceptible to observe possible intensification of aging in the HAZ.

The submerged arc weld was made on a heavy (17 x 37 x 1 1/2 in.) plate section of furnace-cooled steel. The weld preparation consisted of a 1/4-in. radius 2/3-in. deep groove, and welding conditions were as follows: 1/4 - in. diameter electrode of 0.13% C, 2.0% Mn grade, 1000 amp., 38 v., travel speed 9 in. per minute. Thermocouples were flash welded to the bottom of small drilled holes spaced at various distances from the expected fusion line. High-speed, string-galvanometer instrumentation was used to follow the thermal cycles of the near-weld zone. The resulting thermal data are presented in Fig. 8. Temperatures as high as 1200° F. were reached as far as 0.25 in. from the fusion line. Micro-tensile specimens were machined as shown in Fig. 6.

Inasmuch as only the notched area was loaded above the elastic limit during the tensile tests the results obtained indicate the properties of the material confined to the notch zone. The test results are given in Fig. 7 (*B*). It is observed that aging is developed only in the weld and at weld-fusion-line locations (note that the fusion-line location also contained weld metal). Thus, it may be concluded that aging was not induced in the HAZ by the thermal cycle of the weld. Evidently the cooling rate of 10° F. per second in the 1200–800° F. range which characterized this weld was not sufficiently rapid to retain an enforced solution of the aging elements.

Bead-on-plate weld tests were made on furnace-cooled and also normalized plates of approximately 6 x 12 x 1 1/4 in. Welding conditions were as follows: 5/16 in. diameter, E6010 electrode, 175 amp., 26 v., 6 in. per minute travel speed. Thermal cycles were obtained following

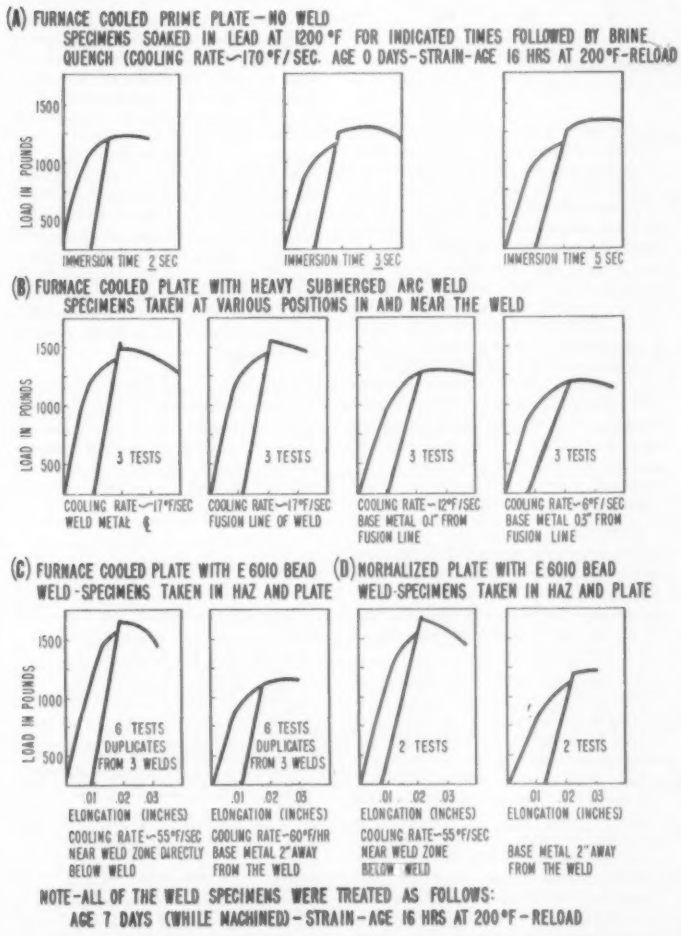
(A) FURNACE COOLED PRIME PLATE – NO WELD
SPECIMENS SOAKED IN LEAD AT 1200 °F FOR INDICATED TIMES FOLLOWED BY BRINE QUENCH (COOLING RATE ~ 170 °F/SEC. AGE 0 DAYS – STRAIN-AGE 16 HRS AT 200 °F – RELOAD
LOAD IN POUNDS
1500
1000
500
IMMERSION TIME 2 SEC
IMMERSION TIME 3 SEC
IMMERSION TIME 5 SEC
(B) FURNACE COOLED PLATE WITH HEAVY SUBMERGED ARC WELD
SPECIMENS TAKEN AT VARIOUS POSITIONS IN AND NEAR THE WELD
3 TESTS
COOLING RATE ~ 17 °F/SEC WELD METAL ℄
COOLING RATE ~ 17 °F/SEC FUSION LINE OF WELD
COOLING RATE – 12 °F/SEC BASE METAL .01" FROM FUSION LINE
COOLING RATE ~ 6 °F/SEC BASE METAL .03" FROM FUSION LINE
(C) FURNACE COOLED PLATE WITH E 6010 BEAD WELD - SPECIMENS TAKEN IN HAZ AND PLATE
(D) NORMALIZED PLATE WITH E 6010 BEAD WELD - SPECIMENS TAKEN IN HAZ AND PLATE
6 TESTS DUPLICATES FROM 3 WELDS
2 TESTS
.01 .02 .03
ELONGATION (INCHES)
COOLING RATE ~ 55 °F/SEC NEAR WELD ZONE DIRECTLY BELOW WELD
COOLING RATE ~ 60 °F/HR BASE METAL 2" AWAY FROM THE WELD
COOLING RATE ~ 55 °F/SEC NEAR WELD ZONE BELOW WELD
BASE METAL 2" AWAY FROM THE WELD
NOTE – ALL OF THE WELD SPECIMENS WERE TREATED AS FOLLOWS:
AGE 7 DAYS (WHILE MACHINED) – STRAIN – AGE 16 HRS AT 200 °F – RELOAD

Fig. 7 – Summary of micro-tensile-specimen data, Al-killed steel only

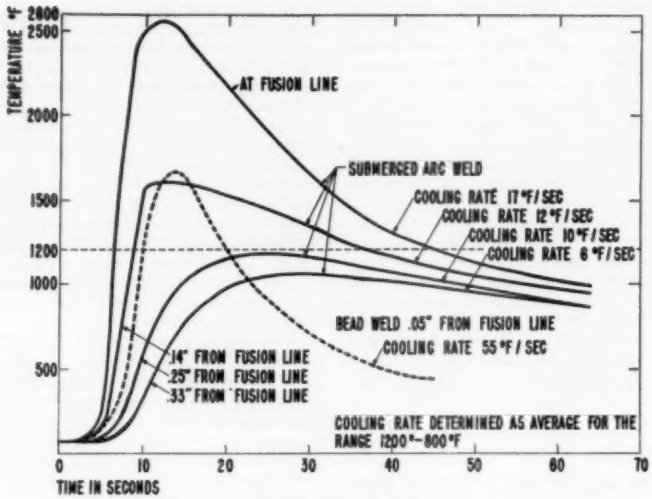

Fig. 8 Weld thermal cycles

the procedure outlined for the submerged arc weld.

The thermal data are presented in Fig. 8, the sectioning details in Fig. 6, and the micro-tensile results in Figs. 7 (*C*) and 7 (*D*). It is observed that the HAZ of the furnace-cooled base material developed marked strain aging in the several tests which were run. No aging was developed by the portion of the plate removed from the weld zone inasmuch as this material had been stabilized by the slow-cooling prior treatment. Thus, an increase in cooling rate from 10° F. per second to 55° F. per second, obtained by a change in welding conditions, resulted in the development of aging potential in the subject steel which was otherwise nonaging. It is interesting to note that the cooling rate of the quench-cooled 0.505 tensile specimens was 80° F. per second (measured at the specimen center). It would be interesting to deduce the exact value of the critical cooling rate between 10 and 55° F. per second which results in enforced solution of aging elements. This would be particularly important since this range covers a considerable fraction of the range of cooling rates obtained in normal welding practice.

Figure 7 (*D*) presents data for the bead-welded normalized material which showed moderate aging for the subject steel. There are indications of intensified aging potential in the HAZ as compared to the base plate; however, the difference in increased reloading yield points is not sufficient to warrant a definite conclusion without further tests.

GENERAL CONSIDERATIONS

The general engineering concept that fully killed structural steels are essentially nonaging requires reanalysis in regard to welded structures. In so far as the portion of the structure which is not significantly affected by the heat of welding is concerned, the concept is nearly exact. The usual normalized grades of commercial, fully killed steel show relatively little aging tendency (following the normalizing treatment) in comparison to semikilled or rimmed steels. The concept, however, is erroneous in so far as near-weld positions in welded structures of fully killed steels are concerned. The extent to which such steels tend to aging susceptibility in near-weld zones has been shown to depend on welding practice. Fast cooling welds promote high aging susceptibilities with resultant embrittlement effects which accompany the development of aging.

Semikilled and rimmed steels which are known to be aging susceptible in the commercial normalized condition are also detrimentally affected by rapid cooling. In this case the result is an intensification of the normal level of aging with consequent added embrittlement.

The generally unfavorable position of welds in structures which has been described in the introductory section of this report magnifies the importance of such embrittlement out of proportion to the relatively small volumes of metal which are affected. It is apparent that embrittling aging reactions must be considered as important variables to the problem of weld performance irrespective of deoxidation practice of the prime material. Depending on welding conditions, embrittlement in the HAZ may develop by quench aging following fabrication. If the structure is of a type which undergoes shakedown, small amounts of deformation which occur at highly stressed weld connections will lead to added strain-aging embrittlement. The combined aging reactions may well induce or aid in the propagation of failures for borderline cases.

Certain anomalous behavior of welded structures may possibly be re-examined in view of these findings. As an example it may be cited that in some cases welded "big-inch" pipe lines have been known to withstand high-proof pressures without failure. Failure, however, occurred subsequently on building up to working pressures considerably below proof levels. The presence of flaws which were not made operative until prestraining and strain aging developed added embrittlement on the circumferential welds provides a possible explanation for the anomalous behavior.

The extensively debated question as to the causes of time-storage improvement for notched bead-on-plate weld tests appears to deserve a reopening. It will be recalled[9] that increased bend angles, indicating improved ductility, were obtained up to about 30 days after welding. Periods of a few hours at 212° F. appeared to accomplish an equivalent effect. The debate has developed two schools of opinion, one of which considers the gradual escape of hydrogen as the cause while the other believes a gradual decrease of aging effects to be responsible. This latter concept is founded on the belief that the metastable aging condition (strain-aging potential) is dissipated by an alternate time-dependent precipitation of inocuous form (possibly quench aging).

In view of the present findings that aging reactions, irrespective of type, produce embrittling effects and that prolonged holding up to 30 days produces increased quench-aging embrittlement it is impossible to reconcile time-storage improvement with aging.

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The Micro-Mechanism of Fracture in the Tension Impact Test

Supplementary Report No. 1

Tension impact tests of rimmed and killed structural steels in which a spheroidizing heat treatment has eliminated lamellar carbide structure

by Walter H. Bruckner

INTRODUCTION

THE prior research of which this paper is an extension was presented by the author in the September 1950 issue of THE WELDING JOURNAL. The earlier paper described and illustrated the initial phases of fracture in the transition region as originating in the carbide lamellae of pearlite grains which were unfavorably oriented to the maximum principle stress for slip to take place.

It was considered a logical extension of the earlier work to attempt to discover the mechanism of fracture of steels in which the lamellar carbides had been replaced by those of spheroidal shape. Accordingly this paper describes the tension impact tests made on a rimmed steel and a killed steel of structural grade in which a spheroidizing heat treatment had eliminated all evidence of the originally lamellar carbide structure.

It was also of interest to study the fracture mechanism of a material substantially free from carbides such as high purity iron, Puron or Armco iron. At the time the program was started the Armco iron was the only material available in the size required for the tension impact specimens. Accordingly this paper describes the results of tension impact tests of Armco iron although work is now underway on the high purity iron which it was possible to obtain later in the program.

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Scheduled for presentation at the Thirty-Second Annual Meeting, A. W. S., Detroit, Mich., week of Oct. 15, 1951.

MATERIALS AND PROCEDURE

The chemical analyses of the rimmed Steel E, the killed Steel Dr and the Armco iron which were tested in impact are given in Table 1.

For the steels E and Dr which were originally in the hot-rolled condition the spheroidizing heat treatment was evolved from pilot tests and required the following schedule:

(*a*) Austenitize at 1650° F., hold 1 hr. and quench in cold water.

(*b*) Reheat to 1280° F. for a total time of 216 hr. and quench in cold water.

(*c*) Reheat to 400° F. for 24 hr. and cool in air after cooling in furnace to 300° F.

Step (*b*) above produced complete spheroidization and eliminated the original lamellar distribution of the carbides. The final hardness of the spheroidized material was approximately the same as that of the as-rolled material with which it was to be compared. The average ferrite grain size after spheroidization was approximately half the diameter of the original as-rolled material.

Step (*c*) above was designed to eliminate the observed slight, quench-aging tendency by resort to overaging at 400° F.

The Armco iron was purchased in the cold-rolled condition and was heat treated according to the follow-

Table 1

Material	*Chemical analyses of materials*											
	C	*Mn*	*P*	*S*	*Si*	*Al*	*Ni*	*Cu*	*Cr*	*Mo*	*Sn*	*N²*
Rimmed Steel E	0.20	0.33	0.013	0.020	0.01	0.009	0.15	0.18	0.09	0.018	0.024	0.005
Killed Steel Dr	0.22	0.55	0.013	0.024	0.21	0.020	0.16	0.22	0.12	0.022	0.023	0.006
Armco Iron	0.02	0.01	0.004	0.025	..	..	..	0.12	..	..	..	..

ing schedule: normalized by heating to 1700° F., held 20 min. at temperature, air-cooled then reheated to 1100° F. and furnace-cooled for stress relief.

Tension impact specimens of the same size and shape of notch were machined from the heat-treated materials as was described in the earlier paper for the Type B specimen with a notch of 0.039 in. radius and a minimum diameter of 0.294 in.

For the spheroidized Steels E and Dr the tension impact tests were made with an initial energy of 220 ft.-lb. for one series of tests, 45 ft.-lb. initial energy for another series and lower than 45 ft.-lb. initial energy for partial fracture tests.

For the Armco iron the tension impact tests were made with an initial energy of 220 ft.-lb., 110 ft.-lb. and 45 ft.-lb. over a range of temperature to give the complete energy-temperature curves. Tests were also made with less than 45 ft.-lb. initial energy to give partial fractures for metallographic examination.

DATA OBTAINED

The results of tension impact tests of spheroidized Steels E and Dr are given in Figs. 1 and 2. For the Armco iron the impact data are given in Fig. 3. The tabulated data for the impact tests are not given in the paper in order to conserve space but they can be made available by request.

The microstructure of the spheroidized Steels E and Dr and of the Armco iron are given in Fig. 4 for the condition the materials were in for the impact tests.

Other data are given in the paper and are best described in following sections.

DISCUSSION OF THE DATA ON SPHEROIDIZED STEELS

In Figs. 1 and 2 the data for the spheroidized steels

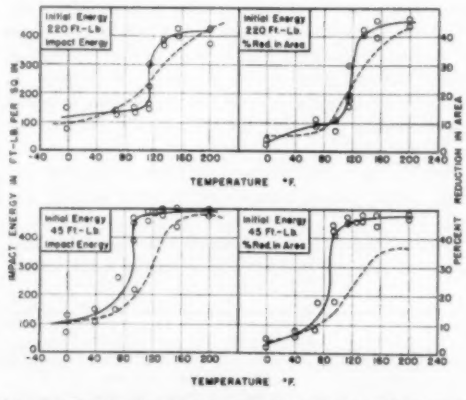

Initial Energy 220 Ft.-Lb. Impact Energy
Initial Energy 220 Ft.-Lb. % Red. in Area
Initial Energy 45 Ft.-Lb. Impact Energy
Initial Energy 45 Ft.-Lb. % Red. in Area
IMPACT ENERGY IN FT.-LB PER SQ. IN.
PERCENT REDUCTION IN AREA
TEMPERATURE °F.

Fig. 1 Tension impact tests and per cent reduction of area, Series 15, Type B specimens of rimmed Steel E in spheroidized condition; 220 and 45 ft.-lb. initial energy. Dotted curves are for as-rolled material

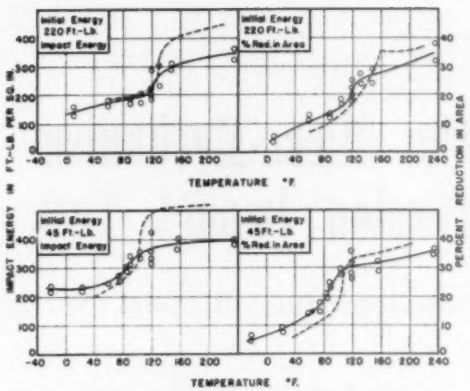

Initial Energy 220 Ft.-Lb. Impact Energy
Initial Energy 220 Ft.-Lb. % Red. in Area
Initial Energy 45 Ft.-Lb. Impact Energy
Initial Energy 45 Ft.-Lb. % Red. in Area
IMPACT ENERGY IN FT.-LB PER SQ. IN.
PERCENT REDUCTION IN AREA
TEMPERATURE °F.

Fig. 2 Tension impact tests and per cent reduction of area, Series 15, Type B specimens of killed Steel Dr in the spheroidized condition; 220 and 45 ft.-lb. initial energy. Dotted curves are for as-rolled material

(solid lines) have been compared with similar data for the same steels in the original, hot-rolled condition (dotted lines). The transition temperatures estimated from the curves are given in Table 2. As shown in Table 2 the transition temperatures for the spheroidized steels were lower than for the corresponding as-rolled steels containing the lamellar carbides when the initial energy of the impact test was 45 ft.-lb. At 220 ft.-lb. initial energy level there was no difference in the transition temperatures for Steel E between the spheroidized and as-rolled condition. However, for Steel Dr at the 220 ft.-lb. level there was a decrease of 10 to 25° F. in transition temperature upon spheroidization. The

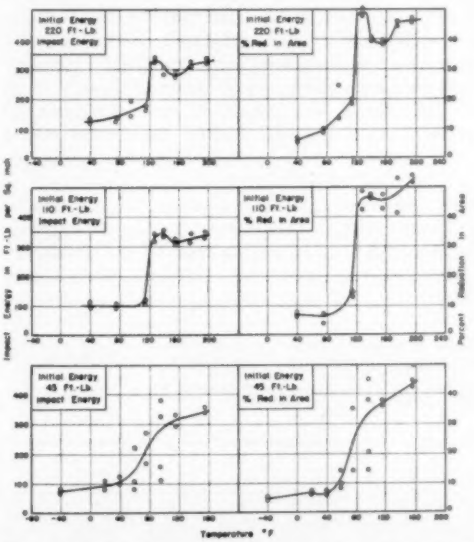
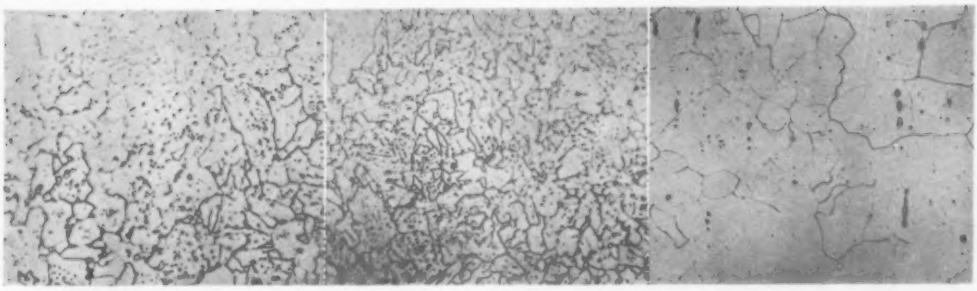

Initial Energy 220 Ft.-Lb. Impact Energy
Initial Energy 220 Ft.-Lb. % Red. in Area
Initial Energy 110 Ft.-Lb. Impact Energy
Initial Energy 110 Ft.-Lb. % Red. in Area
Initial Energy 45 Ft.-Lb. Impact Energy
Initial Energy 45 Ft.-Lb. % Red. in Area
Impact Energy in Ft.-Lb. per Sq. Inch
Percent Reduction in Area
Temperature °F

Fig. 3 Tension impact tests and per cent reduction of area, Series 17, Type B specimens of Armco iron; 220, 110 and 45 ft.-lb. initial energy



Steel E, 500 × Steel DR, 500 × Armco iron, 200 ×

Fig. 4 Microstructure of spheroidized Steels E and Dr and of Armco iron

transition temperature of Steel E as spheroidized was about 30° F. lower for the 45 ft.-lb. level than for the 220 ft.-lb. initial energy level. In the as-rolled condition Steel E showed no tendency toward change of transition temperature with change in initial energy.

For the spheroidized Steel E at the 45 ft.-lb. level there was a large difference in the reduction of area compared with the as-rolled steel as is quite evident in the ductile region above 100° F. This large difference in ductility is not consistently reflected in a difference in the energy absorption.

Table 2—Estimated Transition Temperatures of Steels E and Dr

		Transition temperature in ° F. from:		
	Initial energy, ft.-lb.	*Impact energy*	*Reduction of area*	*Reduction of area 15%*
Steel E:				
As-rolled	220	120	125	105
	45	120	125	105
As-spheroidized	220	120	120	115
	45	90	90	80
Steel Dr:				
As-rolled	220	55	55	45
	45	25	20	15
As-spheroidized	220	45	40	20
	45	10	5	−10

Figures 5 and 6 have been prepared to provide a comparison of the two spheroidized steels for the two initial energy levels. The distinctly lower transition temperature for Steel Dr than for Steel E is at once evident from both curves based on energy absorption and on per cent reduction of area. The generally higher values for the 45 ft.-lb. initial energy are also in evidence. The displacement of the curve for the 45 ft.-lb. level to the left of the curve for the 220 ft.-lb. level appears to be nearly the same for both steels and for both energy absorption and per cent reduction of area. The sensitivity of the change in the transition temperature with a change in initial energy is the same for the two spheroidized steels. However in the as-rolled condition only Steel Dr exhibited this sensitivity.

The most marked change in Steel E due to spheroidization was the increase in ductility (at 45 ft.-lb. initial energy) as compared with the as-rolled material which contained lamellar carbides. The killed Steel Dr showed little change in ductility due to spheroidization; however, the energy absorption was considerably lower than for the as-rolled material.

The relative position of the transition temperatures for the rimmed Steel E and the killed Steel Dr, Figs. 5 and 6, is not appreciably affected by spheroidization and the accompanying smaller ferrite grain size. The indication of the transition from ductile to brittle behavior, Figs. 1 and 2 is made more pronounced by spheroidization of the rimmed steel and less pronounced by spheroidization of the killed steel as compared with their transition behavior in the as-rolled state.

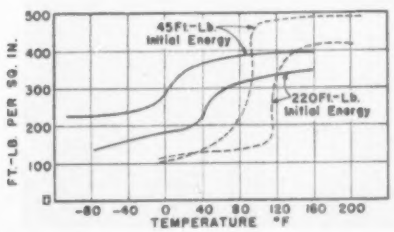


Fig. 5 Comparison of energy-temperature curves of spheroidized Steel Dr (solid lines) and Steel E (dotted lines)

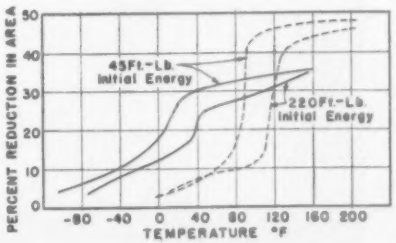
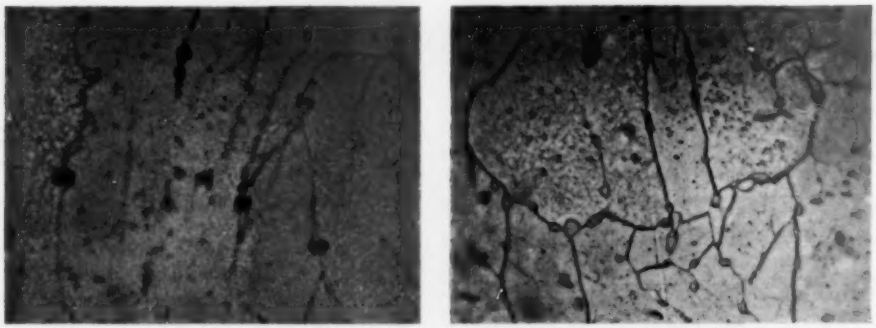


Fig. 6 Comparison of per cent reduction of area-temperature curves of spheroidized Steel Dr (solid lines) and Steel E (dotted lines)

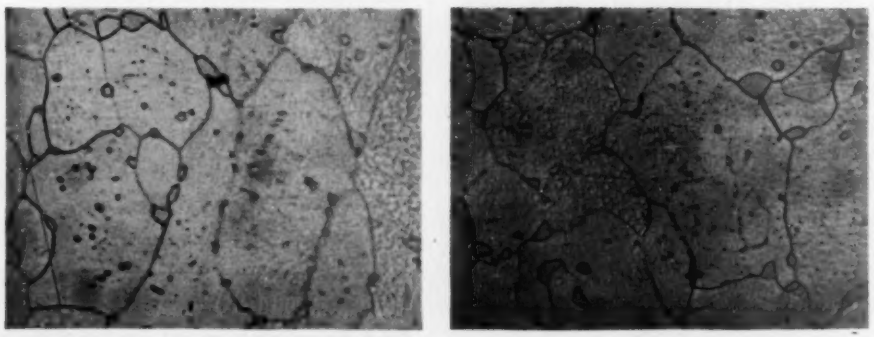
Table 3—List of Partial Fracture Tests of Spheroidized Steels

Spec. No.	Testing temp., ° F.	Initial energy, ft.-lb.	Energy absorbed — Ft.-lb.*	Energy absorbed — Ft.-lb. per sq. in.†	Reduction of area, %	Observation notes on microstructure
			Steel E, transition temperature 90° F.			
29	155	34.8	31.6	466	44.0	Carbide fractures, large amount of shear
35	155	33.2	..	(405)	38.0	Large number of carbide fractures, a few large holes
43	95	27.0	..	(320)	28.0	Large number of cracks in carbides at grain boundaries
48	95	30.2	..	(365)	33.5	Large number of cracks in carbides at grain boundaries
19	20	8.5	..	(90)	2.0	No evidence of any damage
28	20	10.0	..	(100)	3.0	Grain boundary cracks through carbides, no cleavage
31	20	11.5	9.6	142	4.5	Many cleavage cracks, a few twins
			Steel Dr, transition temperature 10° F.			
52	155	27.2	26.6	393	42.5	Ring fractures in carbides and small holes
54	155	26.3	25.5	386	33.0	Ring fractures in carbides and small holes
116	155	25.2	24.6	363	22.5	Ring fractures and cleavage, lacy pattern of many holes
45	155	24.2	..	(380)	33.0	Shear areas with cleavage cracks through carbides
14	10	21.5	..	(275)	15.5	Small shear areas, few carbide cracks
16	10	24.0	22.9	338	24.5	Large number of holes
53	10	23.0	..	(290)	18.0	Large number of diamond-shaped holes, carbide cracks, no cleavage
114	−40	16.0	13.5	199	9.5	Highly fragmented, carbide cracks found at start of cleavages
115	−40	14.5	..	(215)	6.5	A few cleavage cracks in center

* Energy value is given in this column only for specimens which fractured completely.
† Energy values given in brackets taken from energy-per cent reduction of area curves for 45 ft.-lb. initial energy.



Figs. 7 and 8 Spheroidized Steel E, Specimen SE48 tested at 95° F. Absorbed 445 ft.-lb./in.² with 33.5% R. A. Etchant 5% Nital, magnification 1500 ×



Figs. 9 and 10 Spheroidized Steel E, Specimen SE43 tested at 95° F. Absorbed 398 ft.-lb./in.² with 28% R. A. Etchant 5% Nital, magnification 1500 ×

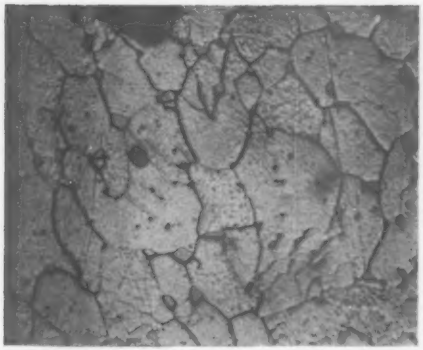


Fig. 11 Spheroidized Steel Dr, Specimen SD54 tested at 155° F. Absorbed 386 ft.-lb./in.² with 33% R. A. Etchant 5% Nital, magnification 1500 ×

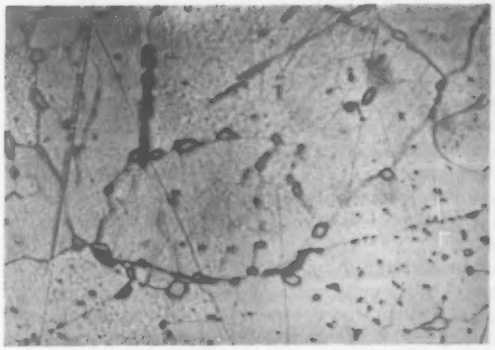


Fig. 12 Spheroidized Steel E, Specimen SE31 tested at —20° F. Absorbed 142 ft.-lb/in.² with 4.5% R. A. Etchant 5% Nital, magnification 1500 ×

PARTIAL FRACTURE TESTS OF SPHEROIDIZED STEELS

In order to clarify the fracture mechanism of the spheroidized steels a number of specimens of both steels in the spheroidized condition were subjected to partial fracture tests at temperatures above, below and in the transition region. Table 3 describes the test conditions and also gives the notes made while having the polished and etched specimens under observation with the microscope.

Table 3 shows that one of the first manifestations of fracture is the cracking of the spheroidal carbides situated at grain boundaries while those within the ferrite grains are not fractured until considerably later in the fracture sequence or at low temperatures in or below the transition region. These micro-cracks in the spheroidal carbides at grain boundaries are illustrated in Figs. 7 to 10 for the rimmed Steel E tested in impact above and in the transition region; in Fig. 11 for killed Steel Dr above the transition region and in Fig. 12 for Steel E below the transition region. All of the photomicrographs are so oriented on the page that the direction of the principal stress during the impact is up and down. It is evident from the micrographs that most of the traces of the micro-cracks are perpendicular to the principal stress direction. Figure 12 shows how cleavage fractures in the ferrite are generated by the high stresses produced at the ends of the micro-cracks in the carbide spheroids. This is typical of fracture in the brittle range for the spheroidized steels tested.

It is especially noteworthy that little or no evidence of mechanical twinning was established in the fracture sequence of the spheroidized steels. The detection of twins was extremely difficult due to the small ferrite grain size and wide distribution of the spheroidal carbides. The reason that twins were not prevalent is considered to be that the twinning operation is a stress-relieving mechanism which occurs when slip is inhibited and the stress is not high enough to produce cleavage whereas, with micro-cracks present in large numbers in the spheroidal carbides well distributed throughout the stressed volume of metal in the notch, stress relief occurs at the ends of the micro-cracks either by slip, at high temperatures or by cleavage at lower temperatures. The micro-cracks are considered to afford sufficient opportunity for localized stress relief thus making it unnecessary for the ferrite to twin in response to the applied stress.

A most unusual feature of the fracture of the spheroidized Steel Dr in the ductile region above the transition temperature is illustrated in Fig. 13 for Specimen 116 which was fractured at 155° F. Figure 13 shows an odd-shaped ring of fracture caused by linking up of the micro-cracks in the carbide spheroids. The entire notch volume is filled with such rings which are designated as "ring fracture" in Table 3. Since only the material at the periphery of these rings is strained the circumstance offers an explanation of the lower energy absorption of the spheroidized Steel Dr as compared with the as-rolled material. It is not apparent as yet why this peculiar fracture should be a feature of Steel Dr's behavior.

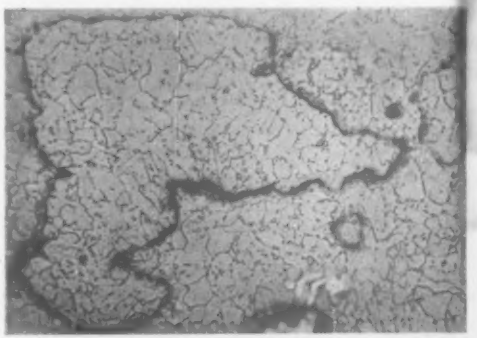


Fig. 13 Spheroidized Steel Dr, Specimen SD116 tested at 155° F. with 363 ft.-lb./in.² and 22.5% R. A. Etchant 5% Nital, magnification 250 ×

DISCUSSION OF THE DATA ON ARMCO IRON

The tension impact data given in Fig. 3 for the Armco iron show a characteristic peak in the curves at the high-temperature end of the transition region for specimens tested with initial energies of 220 and 110 ft.-lb. The data for specimens tested with 45 ft.-lb. initial energy also show peak values at the top of the transition region but other specimens tested at the same temperature gave extremely low values, thus the curves were drawn through the median value. It is to be noted, however, that both energy absorption and per cent reduction of area show the peak value at the same temperature, thus suggesting that the specimens tested at the temperature coincident with peak energy and ductility were in some manner able to undergo a larger than normal degree of plastic deformation.

In order to determine the relative absorption of strain energy of specimens exhibiting peak energy and ductility and of specimens tested at lower and higher temperatures than coincident with the peak, Vickers hardness tests were undertaken. The specimens chosen for study were those fractured at 110 ft.-lb. initial energy at temperatures of 155, 127 and 115° F. It will be seen from Fig. 3 that these temperatures are, respectively, above the peak, at the peak and below the peak value for the tests made with 110 ft.-lb. initial energy. These specimens were sectioned longitudinally along a diametral plane and were then prepared for metallographic examination. Upon completion of metallographic observations hardness tests were made along the longitudinal center line of the specimens with a Vickers diamond pyramid and a load of 500 gm.

The metallographic examination revealed a maximum population of mechanical twins in the specimen corresponding to the peak energy absorption. A count of the number of twins in the three fractured specimens on the diametral plane of observation gave 70, 1700 and 110 twins, respecitively (in round numbers), for the specimen broken at 155, 127 and 115° F., thus establishing that maximum twin density and peak energy were coincident.

The results of hardness tests are given in Fig. 14 which contains the three hardness contours superimposed on the same scale for comparison. The indications from Fig. 14 are that the strain energy absorbed by the three specimens increased consistently as the temperature of the impact test was increased. Peak energy absorption in impact at 127° F. was therefore not substantiated by the hardness tests along the longitudinal center line of the specimens. Extensive and laborious hardness surveys were then undertaken covering one half of the specimen's surface (specimen tested at 127° F.) from the longitudinal center line to the bottom of the notch. These surveys are not illustrated but they revealed a steep strain gradient of increasing hardness in going from the center of the specimen toward the bottom of the notch. The maximum hardness for Specimen 6N2, tested at 127° F., was 193 Vickers as compared with 175 Vickers for the Specimen

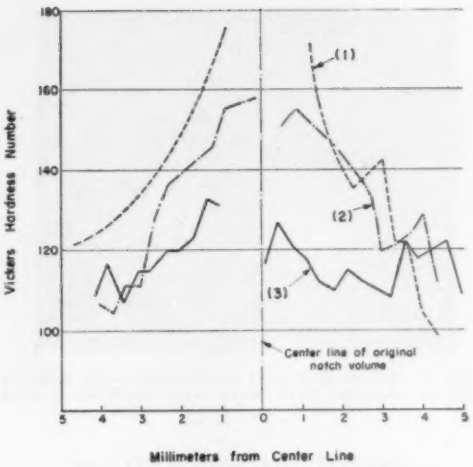


Fig. 14 Results of hardness surveys along longitudinal center line of Specimens 1BN5, 6N2 and 4BN2 which were fractured at, respectively, (1) 155° F., (2) 127° F. and (3) 115° F., with 110 ft.-lb. initial energy fracture is shown by break in curves

1BN5 tested at 155° F. The strain energy which is responsible for the peak in the impact-temperature and per cent reduction of area-temperature curves evidently must have been contributed by the material close to the bottom of the notch or at least away from the center of the specimen since the hardness survey at the center line shown in Fig. 14 gave a lower maximum hardness and a less steep strain gradient, longitudinally for the specimen tested at 127° F. than for the test at 155° F.

It is postulated that the peak in the impact-temperature curves is due to early failure in the center of the specimen in accordance with the increase in shear restraint with decrease in temperature. A higher order of plastic strain in a remaining tubular section of the notch volume surrounding the central fracture was then made possible by the collapse of the central restraint due to opposing stresses. This higher order of plastic strain is conceived of as taking place in a system of practically uniaxial stress. In order to test the above hypothesis it was necessary to determine whether the large number of mechanical twins formed in the specimen representing peak energy absorption did or did not contribute to the energy absorption peak and to the increased ductility. Incidentally the hardness tests which were made showed that the twinned regions had absorbed a large amount of strain energy which apparently was due to shear strain undergone subsequent to twin formation thus establishing that twins in ferrite are ductile under suitable conditions of stress. Hardness tests were undertaken to determine the relative hardness of twins and of surrounding ferrite in any one grain using the Hanemann micro-hardness tester which could be employed with loads as low as 1 gm. This application was unsuccessful because the twins were so

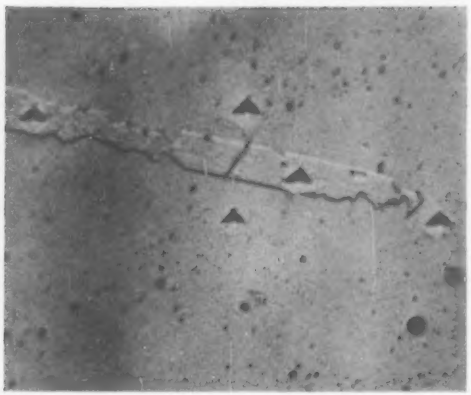


Fig. 15 Micro-hardness indent made in large twin and surrounding ferrite with 1²/₃ gm. load on the indentor. Magnification 900 ×

small that it was impossible to confine the indentation to the twin region.

Several bars of the Armco iron were then put through a strain-annealing process to produce extremely large grains of ferrite in specimens machined to the desired shape and size of notch after straining but before annealing. These specimens were tested in impact to give brittle fracture and large, broad mechanical twins suitable for hardness tests. The large mechanical twins in the specially prepared Armco iron were indented with a load of 1²/₃ gm. The small indentation produced could be accurately placed in the center of a twin, in the surrounding ferrite of the grain or at the twin interface. From the appearance of the indentations it was deduced that the twin region was no harder than the surrounding ferrite except at the twin interface which appeared just slightly harder. Figure 15 which is typical of the many twins thus surveyed shows that the indents within the twin are of the same size as those in the surrounding ferrite. Back-reflection X-ray diffraction studies (performed by Prof. J. W. Marx, Assistant Professor of Physical Metallurgy) of the large grains containing twins also showed only very slight asterism in the Laue-pattern spots associated with the twin orientation, thus confirming the other evidence of a generally low order of energy absorption. It was thus evident that the twinning operation could not account for the peak in the impact-temperature curves of Armco iron and that the extra energy must have been due to shear suffered by the notch volume subsequent to twinning. A large amount of elastic energy is, however, involved in initiating and propagating the twins and it might be thought that this energy could account for the impact peak. That this is not the case is believed to be shown by the fact that ductility is also at peak value simultaneously with energy.

Another thought which should be interposed at this stage is the question, "What bearing does the evidence of simultaneity of maxima of energy, ductility and twin density have on the argument advanced in the previous paper regarding the role of mechanical twinning in nucleating cleavage fracture?" The evidence is indisputable in the prior report that twins and cleavage are interrelated and in the present instance that twins are ductile. The conflicting evidence regarding twins can be resolved by consideration of the instantaneous state of stress in the regions where the twins are initiated and into which they propagate. It begins to appear from metallographic evidence that if cleavage results from twin formation it can occur only during the time the twin is being propagated and reaches some barrier which causes the elastic energy involved in twinning to be dissipated at the barrier. Some of these barriers are now being studied with the electron microscope. The preliminary metallographic studies are given in the following section.

METALLOGRAPHIC OBSERVATIONS OF FRACTURE IN ARMCO IRON

One of the specimens of Armco iron tested in impact, Specimen 3N1, had fractured outside of the region of maximum stress and the fracture had been located in a region where the density of mechanical twins was a maximum. This specimen was illustrated in the macrograph of Fig. 20 in the previously published paper and was considered worthy of a more intensive study than it had been given. The objective in making a more careful survey was to attempt to correlate the twinning operation with the failure by cleavage through the twinned area exhibited by this specimen.

The micrographs which were recorded in the survey of the above specimen are given in Figs. 16 to 24, which are so oriented that the principal stress is up and down on the page. Figure 16 shows a mechanical twin which traverses one entire ferrite grain from one grain boundary to another. The specimen had been etched to

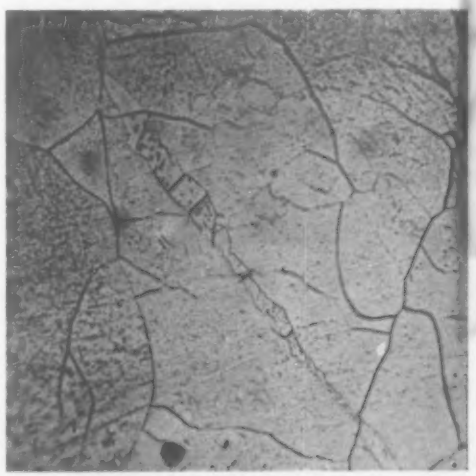


Fig. 16 Armco iron, Specimen 3N1, etchant 5% Nital, magnification 1500 ×

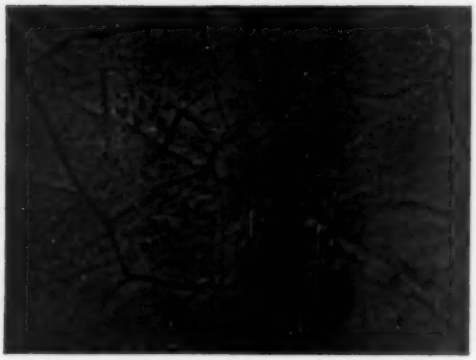


Fig. 17 Armco iron, Specimen 3NI, etchant 5% Nital, magnification 2000 ×

clearly show the subboundary structure delineated by the alpha veining. Figure 16 was recorded with the fine structure in the twin focused sharply and the fine structure consists of small etch pits which show the differing orientation of each cell of the substructure through which the twin propagated. The former boundaries of the substructure cells now in the twin region are etched out to an exaggerated degree showing that in the twin these boundaries are regions of high energy which are rapidly attacked by the etchant. It is to be noted that the shape of the twin is caused to change by each cell of the substructure through which the twin progresses, thus a twin region traversing a ferrite grain containing a substructure does not represent homogeneous deformation except within any one cell. The differing orientations of neighboring cells thus accounts for the irregular shape which the twin interface usually has. Figure 17 shows the same twin as in Fig. 16 but etched and photographed especially to show the subboundary detail of the ferrite grain. The differing shear movement in the twin is shown by the offset of the

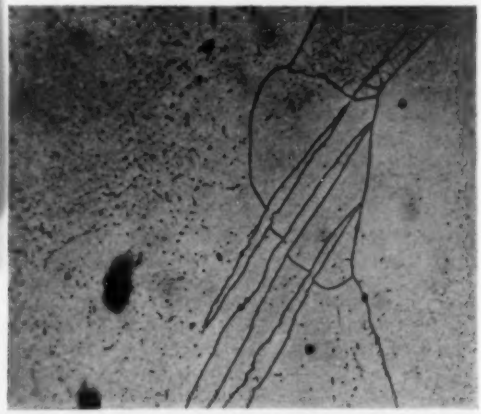


Fig. 18 Armco iron, Specimen 3NI, etchant 5% Nital, magnification 1500 ×. (Reduced 20% in reproduction)

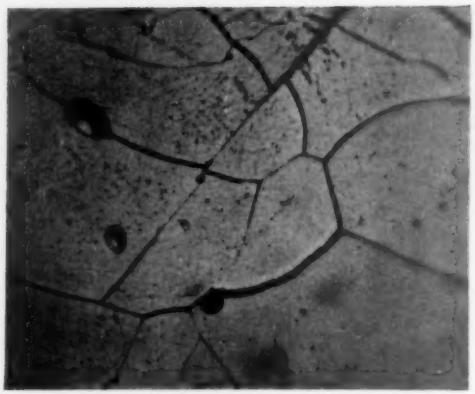


Fig. 19 Armco iron, Specimen 3NI, etchant 5% Nital, magnification 1500 ×

subboundaries. The offset of the ferrite grain boundary as distinguished from the substructure boundaries is shown in Fig. 18 for a series of twins which have propagated into neighboring grains having almost identical orientation. It will be noted that in contrast with the subboundary structure indelibly etched out in the twin of Fig. 16, the ferrite grain boundary traversed by the twins in Fig. 18 has not been etched out in the twins. Other twins which propagated across ferrite grain boundaries of neighboring grains whose orientations differed widely were seen to have the grain boundary well etched out in the twin. However, in no case was it ever found that a twin in propagating across a ferrite grain boundary had initiated a cleavage crack there. In the Armco iron, twins appear to propagate as such into neighboring grains or are stopped at grain boundaries, and cleavage cracks also propagate as such across ferrite grain boundaries once they are started within the grain.

Figure 19 shows a cleavage fracture which traverses a number of ferrite grains and it is to be noted that the two portions of the ferrite grain in the center are not in

Fig. 20 Armco iron, Specimen 3NI, etchant 5% Nital, magnification 1500 ×. (Reduced 20% in reproduction)

Fig. 21 Armco iron, Specimen 3NI, etchant 5% Nital, magnification 2500 ×

register across the cleavage trace. The rest of the boundaries, including the alpha veining structure in this particular field, are in exact registry which suggested to the author that the origin of the cleavage fracture was within the grain having the offset boundaries. A twinning operation in this grain just preceding the cleavage could have been responsible for the observed movement upon removal of the localized stress by cleavage.

Figure 20 shows a stepped cleavage trace in the central ferrite grain and the extension of the cleavage to neighboring grains. Between the widest step of the cleavages in the center grain a twin trace has been etched out whose sides are quite straight. The straight sides suggest that the twin involves a single subboundary cell. The fracture sequence in this field is conceived as involving a prior twinning operation in the center ferrite grain and the propagation of the twin toward the plane of polish from its origin in the material removed in polishing (the twin trace was removed in subsequent polishing to a considerably lower depth). Either due to a weak subboundary or a large difference in orientation of neighboring cells making the further propagation of the twin difficult it is presumed that fracture occurred in the twin at the subboundary thus raising the stress to a value at which cleavage could initiate and propagate into neighboring grains. The sequence of fracture could also have been the reverse of that postulated, e.g., the cleavage fracture could have been first and followed by twinning but this does not appear possible in this particular field since the cleavage would have made it possible for the material between the large step to deform by slip rather than cause twinning.

Figure 21 is a companion to Fig. 20 and shows a series of twin traces arranged in stepped formation. The

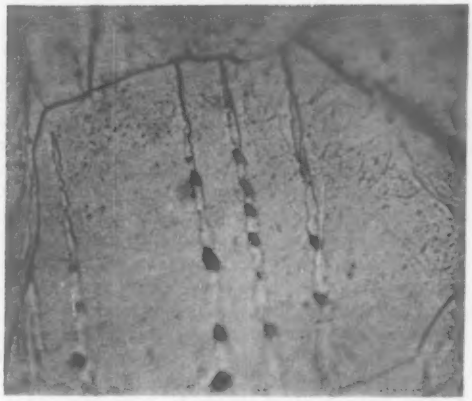


Fig. 22 Armco iron, Specimen 3NI, etchant 5% Nital, magnification 1500 ×. (Reduced 20% in reproduction)

steps occur at the subboundaries due to alpha veining and between the steps are regions which have undergone an accelerated attack of the etching reagent. The etch pits developed in the steps indicate localized high energy absorption due to energy dissipation from the arrested twins. Figure 20 is believed to represent the same condition as shown in Fig. 21 except that the energy was large enough to initiate the cleavage fracture shown in Fig. 20.

Figure 22 shows another field with a large ferrite grain. The grain contains a set of six parallel twins and a stepped cleavage trace from which by means of a Wulff net it was possible to determine that the polishing plane for the large grain was within about 4 degrees of parallel with a 100 face of the unit cube. The twins show characteristic etching patterns indicative of localized energy absorption or fracture and several sets of these patterns are lined up either parallel to or perpendicular to the cleavage traces both of which would be cleavage traces for this particular grain. In the

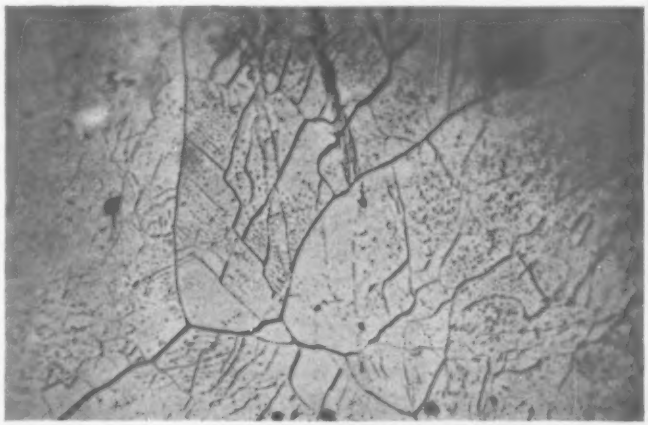


Fig. 23 Armco iron, Specimen 3NI, etchant 5% Nital, magnification 1500 ×

upper ferrite grain at the left side an indistinct twin trace can be seen which has either been stopped or started at the boundary of the central large grain. Three of the etch marks in the twins are on a line parallel with the cleavage trace and this line intersects the grain boundary directly where the twin in the upper grain meets the boundary. It is regrettable that it cannot be established whether the twin was stopped at the boundary of the center grain or was started there since, if the former, it could be expected that a wave of stress traveling across the grain from an interrupted twin operation might have been a consequence and resulted in fracture of the twins already formed.

Figure 23 shows portions of some large ferrite grains in which the alpha-veining substructure has been etched out distinctly. In one of the grains there are several twin traces, one of which can be seen quite clearly changing its shape as it crosses each cell of the substructure. The record of this field is shown because in several of the cells a regular, rectangular array of etch pits can be seen which have a spacing of about 3.2 x 10^{-6} cm. as measured on the print. Measurements made on electron micrographs (by T. S. Noggle, Research Assistant) gave a range of about 2.6 to 3.5 x 10^{-6} cm. for this spacing, and established the micro-etch pits as delineators of the extent of the crystallites or mosaic blocks involved in plastic flow. In some of the cells contiguous with the twin trace the micro-etch pits are arrayed as shown in the sketch of Fig. 24.

The etch pits have a square, rectangular or triangular shape depending upon the orientation of the lattice with respect to the plane of polish. The light microscope does not fully resolve their characteristics, therefore a study of them is now underway by means of the electron microscope. It was noted, however, that at the twin interface there was an occasional indication of pairs of darker etching or deeper pits as shown in the sketch of Fig. 24. This was taken to indicate a higher localized energy absorption as an early stage of cleavage fracture in the twin as illustrated for the final fracture in Fig. 21 of the present paper and in Fig. 21 of the previous report. It was also noted that in the thicker portions of the specimens which were not stressed beyond the yield point there were occasional areas of certain grains which contained the regular array of micro-etch pits. It is therefore indicated that prior plastic deformation is not necessary for the delineation of the crystallites by means of the micro-etch pits.

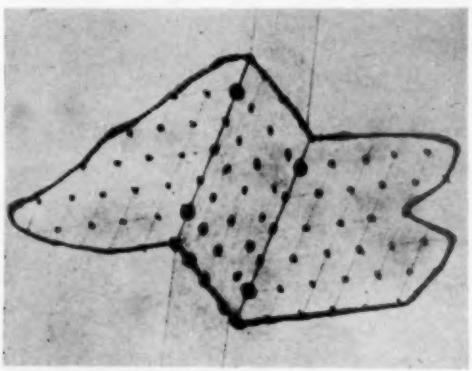


Fig. 24 Sketch showing array of micro-etch pits in twin and neighboring ferrite cell

Solid line, substructure cell boundary. Shaded area, twin trace. Dots represent etch pits. Approximate magnification of 9000 ×

GENERAL SUMMARY AND CONCLUSIONS

Spheroidization of Killed Steel Dr and Rimmed Steel E

The effects of spheroidizing the two steels upon the tension-impact properties at initial energies of 220 and 45 ft.-lb. were compared with the tension-impact properties previously determined for the steels in the as-rolled condition. The following effects of spheroidization were noted:

1. A lower transition temperature was obtained for the spheroidized steels than for the as-rolled steels when tested in impact at an initial energy of 45 ft.-lb. For the spheroidized killed steel the transition temperature was lower than that of the as-rolled material at 220 ft.-lb. initial energy.
2. The transition temperature of the rimmed steel was lower for the 45 ft.-lb. initial energy than for the 220 ft.-lb. initial energy thus providing a behavior similar to that of the killed steel. However, in the as-rolled condition the rimmed steel had the same transition temperature independent of the initial energy used.
3. The most marked change due to spheroidization was the reduction in the energy absorption of the killed steel without appreciable change in ductility and the increase in ductility of the rimmed steel, especially at 45 ft.-lb. initial energy, without an appreciable change in energy absorption.
4. The relative position of the transition temperatures of the rimmed and the killed steels was the same after spheroidization as in the as-rolled condition. The ferrite or the ferrite boundary condition resulting from the deoxidation practice thus appears to be the chief factor in establishing the transition temperature.
5. The initial step in the fracture of the spheroidized steels was noted from partial fracture tests to be the micro-fracture of the spheroidal carbides at the ferrite grain boundaries. The fracture of carbides within the ferrite grain was noted as occurring at a more advanced stage of failure.
6. The general absence of twinning in the ferrite was noted and was ascribed to the stress-relief mechanism afforded at grain boundaries through the cracking of carbide spheroids. The twinning operation was considered to be unnecessary for stress relief or actually prevented, and cleavage initiation was noted as occurring directly from the ends of the micro-cracks in the carbides.

Impact Tests of Armco Iron

1. There was a reduction in the transition temperature of the Armco iron of about 30° F. when the initial energy was 45 ft.-lb. as compared with the transition temperature for initial energy of 220 and 110 ft.-lb.

2. The formation of mechanical twins in regions of low stress at the outer edges of the notch volume was noted as a prominent feature of the fracture of Armco iron in tension impact.

3. Fracture of Armco iron in the region of maximum density of mechanical twinning was illustrated for one of several such specimens while other specimens were shown to exhibit maximum ductility and energy absorption coincident with maximum twin density.

4. Micro hardness tests of mechanical twins indicated that the energy absorption in the twinning operation is of such low level, on the average, that it cannot be considered as directly contributable to the peak energy and ductility observed in impact tests of Armco iron.

5. The peak in energy and ductility has been shown by hardness tests to be related to an intensive absorption of plastic strain energy in a tubular region surrounding the central portion of the notch volume, the latter having fractured earlier in the fracture sequence.

Metallographic Correlation of Mechanical Twinning and Cleavage Fracture in Armco Iron

Evidence has been presented in various photomicrographs which justify the following conclusions:

1. The subboundary structure known as alpha veining is etched out most heavily in a mechanically twinned region and is indicated thereby as a highly localized region of large energy absorption.

2. The initiation of cleavage failure from some disturbed portion of a mechanical twin appears to be confined to regions of the substructure within the ferrite grain. Quite generally it was noted that propagation of cleavage or of twins through a series of neighboring grains takes place as such and a twin has never been found to initiate cleavage at the ferrite grain boundary

ACKNOWLEDGMENTS

This work has been performed as part of the cooperative research contract between the Engineering Experiment Station of the University of Illinois and the office of Naval Research, under Contract N6 ONR-71, Task Order V. All work on this task was under the supervision of Dr. N. M. Newmark, Research Professor of Structural Engineering, University of Illinois.

The tests were conducted in the Department of Mining and Metallurgical Engineering with the assistance of T. S. Noggle, Research Assistant.

Ice Flower-Like Structure in Metal Deposited by Arc Welding in Particular Mild Steel

by Minoru Okada and Yoshikazu Wakabayashi

THE "ice-flower" structure which the authors consider is the columnar structure of the weld metal in which there has been a separation of columnar grains due to the pressure of hydrogen and presence of fine particles of refractory oxides. They believe it to be similar to micro-fissuring discussed in the English welding literature. The structure is associated with and is found in close proximity to fish eyes when the latter appear in weld metal. The report is a condensation of a number of reports made by the senior author over a number of years, the data being presented in tables, charts and curves in the present report.

From theoretical considerations curves are drawn giving the relation in Fig. 1 for hydrogen content of weld metal at $T = 1800°$ K. for various ratios of CO/CO_2 and H_2O/H partial pressures and in Fig. 2 for various FeO contents at different temperatures. Experimental hydrogen analyses are given for weld metal deposited from a variety of electrodes including gas shielded and low hydrogen types. Observation of weld metal from synthetically coated and commercial electrodes showed presence of ice-flower structure if hydrogen content was greater than 0.10 cc./gm. for some electrodes Type A (gas shield), greater than 0.025 cc./gm.

(*Continued on page 472-s*)

Minoru Okada and Yoshikazu Wakabayashi are connected with the Department of Welding Engineering, Osaka University, Osaka, Japan.

A review by W. H. Bruckner of a reprint from Technology reports of the Osaka University, Vol. 1, No. 7, 1951.

A Welded Exposition Building*

by M. Cosandey

THE new exposition building at Lausanne, Switzerland, is of rigid frame construction with a roof of diagonal grid type, Figs. 1 and 2. The roof span is 144 ft. The rigid frames and grid are of welded mild steel St 37, which was selected for its availability and good weldability. Good weldability under biaxial restraint was especially important at the intersections of the arches. Electrodes were used that had no tendency to crack. In order to utilize fully the permissible stress of 45,500 psi. in tension, the upper flange of each arch is of constant thickness, 0.47 in., whereas the lower flange varies in thickness from 0.59 to 1.18 in. The upper flange is protected from buckling by being tied in to the roof, which is covered with hard aluminum alloy sheets.

The columns, Fig. 3, consist of webs 0.31 to 0.47 in. thick with flanges 12 in. wide, 1 to $1^3/_4$ in. thick inside and 0.39 to 0.59 in. thick outside. Double-vee butt welds were used for plates up to $1^3/_{16}$ in. thick. Machined U grooves were used for thicker flanges. Figure 4 shows the method of supporting the gallery beams on the columns. A reinforcing plate is slot welded to the web.

* Abstract of "La Construction métallique de la nouvelle halle d'exposition du Comptoir Suisse à Lausanne," published in *L'Ossature Métallique*, 16, 173–180 (1951) and in *Bull. Technique de la Suisse Romande*, No. 18 (1950).

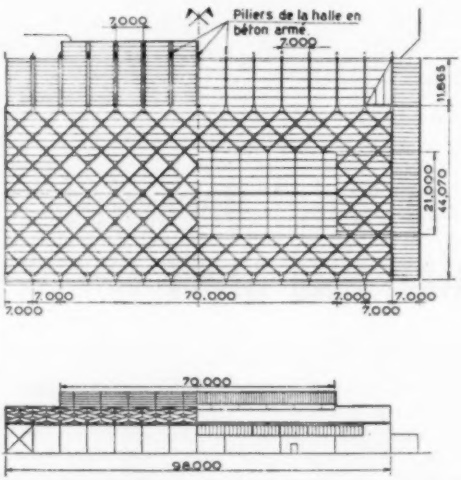


Fig. 1 Plan and longitudinal section of the exposition building. Dimensions in meters. (44.070 meters = 144 ft.)

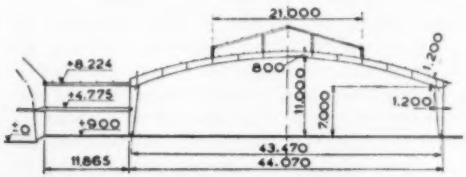


Fig. 2 Transverse section of the building. Dimensions in meters

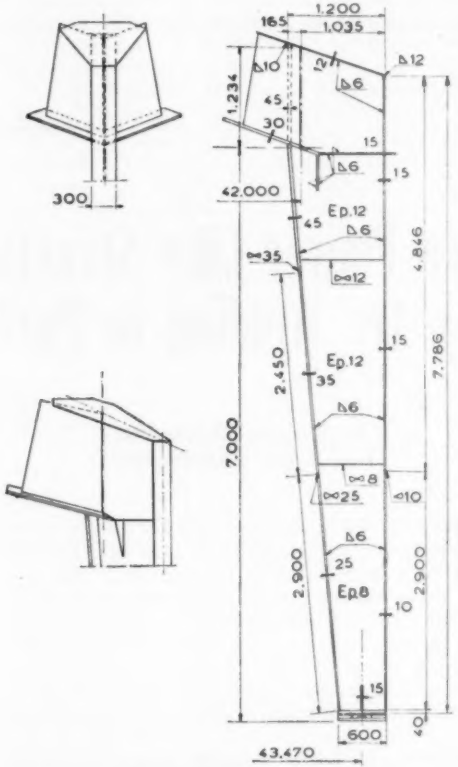


Fig. 3 Details of a column. Dimensions in millimeters. EP = thickness

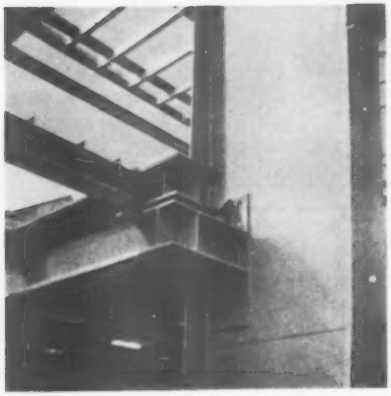


Fig. 4 The support of the gallery beams on the web of a column

work was flame cleaned before painting. Before fabrication was begun a sample arch 200 ft. long was welded upon which shrinkage measurements were made. Longitudinal shrinkage due to welding was negligible. The buckling of the webs was corrected by jacks, with heat applied to the heavier webs. Six principles were helpful in controlling the work:

1. Selection of the best welders only, for principal and secondary welds alike.
2. Maintaining fillet welds to the strict dimension dictated by design calculations.
3. Basing the sequence of welding on a reasoned study of shrinkage.
4. Exact fit of parts to be welded.
5. Welding in the most favorable position; if necessary erecting staging to simplify the dexterity required to complete a weld.
6. Welding the joints symmetrically to reduce angular distortion.

An inside view of the completed building, Fig. 5, shows the diagonal grid of arches. The 400 tons of steel work involving 6600 ft. of field welding were erected in three months. The technical details of the work were taken care of by the firm of Zwahlen & Mayr in Lausanne. The were assisted in the fabrication phases by Ateliers de Constructions Mécaniques de Vevey S.A.

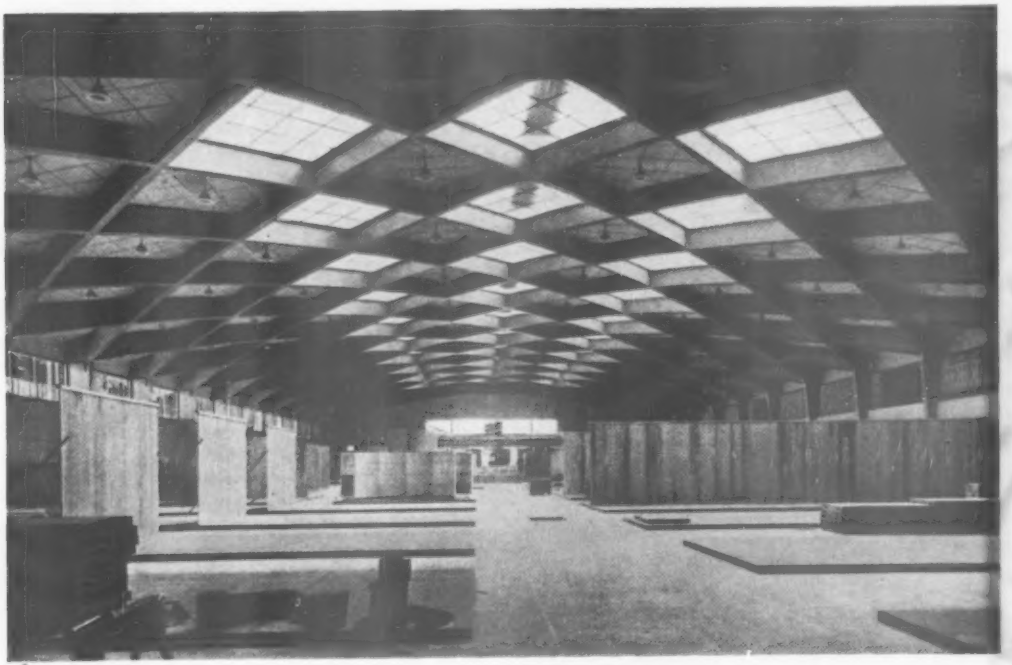


Fig. 5 Interior of the exposition building

The fabrication of the steel work was facilitated by the large number of identical or similar pieces. To produce the desired inclination of the flanges on the webs, guides were tacked on the webs. The thinner flanges were then bent to fit; the thicker flanges had to be heated for bending. The 246 tons of steel in the roof required 22,600 ft. of shop welding. The steel

(*Continued from page 469-s*)

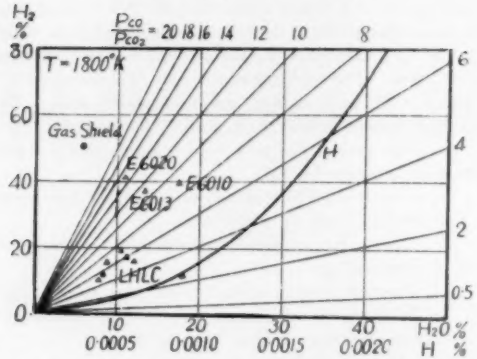


Fig. 1 Relation between arc atmosphere and dissolved hydrogen by our calculation. The experimental values are according to Mallett's (△) and Larson's (○) data

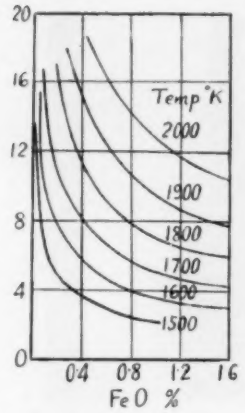


Fig. 2 Relation between FeO and H in molten steel by our calculation

for another type, Type W (slag shield) and in excess of 0.20 cc./gm. for still another, Type B-17 (commercial electrodes). These experiments convinced the authors that it was not hydrogen content of the weld metal alone that caused the fissure problem.

A series of experiments were made with small amounts of steel melted in an Al_2O_3 crucible and cast into a metal mold. The addition of 3% or more of iron oxide to the melt prevented formation of the ice-flower structure. The addition of a small amount of V also prevented ice-flower structure due, as the authors believe, to the tendency of V to form a stable hydride.

By varying the chemical composition of the liquid metal cast into the mold the authors show by means of ternary diagrams that the ice-flower structure is more often formed when the melt is high in Si as compared with Mn and C. Excessive additions of Fe-Si, Al, Cr and Fe-Ti caused the ice-flower structure to appear due to the large amounts of SiO_2, Al_2O_3, Cr_2O_3 and TiO_2 formed which are refractory. These oxides are finely dispersed and are believed to cause rapid evolution of H_2 from the melt. When the deoxidation products have a melting temperature lower than that of the steel no fissures are formed.

The conditions necessary for the formation of the ice-flower structure are a melted supersaturated with hydrogen and containing a fine dispersion of refractory oxides which is subjected to rapid cooling. If either one of the three conditions is absent the flower structure does not appear. Preventative conditions are high Mn content of core wire with Si and Al low, Fe-Mn used as deoxidizer in coating with SiO_2 as low as possible, presence of about 0.03% O_2 in melt, low melting point of deoxidation products and low cooling rate. For gas-shielded electrodes the content of SiO_2 and Al_2O_3 in the coating should be low.

WELDING RESEARCH COUNCIL BULLETINS

The Welding Research Council has inaugurated a series of Bulletins to include reports for which space cannot be provided in the regular Welding Research Supplement. These Bulletins are furnished to the contributors and research workers of the Council and placed on sale at a nominal charge through the AMERICAN WELDING SOCIETY. A list of the Bulletins published to date and their prices are:

No. 1. *Steel Compositions and Specifications*, by Charles M. Parker, March 1949. Price: Fifty cents.

No. 2. *The Nature of the Arc*, by J. D. Cobine, April 1949. Price: Fifty cents.

No. 3. *Burn-Off Characteristics of Steel Welding Electrodes*, by D. C. Martin, P. J. Rieppel and C. B. Voldrich, May 1949. Price: Fifty cents.

No. 4. *Testing Pressure Vessels*, by F. G. Tatnall, and *Effect of Welding on Pressure Vessel Steels*, by A. F. Scotchbrook, L. Eriv, R. D. Stout and B. G. Johnston, February 1950. Price: Fifty cents.

No. 5. *How Plastic Deformation Influences Design and Forming of Metal Parts*, by John R. Low, Jr., and *Brittle Fracture in Mild Steel*, by J. S. Hoggart, May 1950. Price: One dollar.

No. 6. *Stress Corrosion Cracking of Stainless Steels*, by Helmer Nathorst. Part I—Practical Experiences; Part II—An Investigation of the Suitability of the U-bend Specimen, October 1950. Price: One dollar.

No. 7. *Instrumentation for the Evaluation of the Stability of the Welding Arc*, by L. P. Winsor, L. McDonald Schetky and R. A. Wyant, November 1950. Price: One dollar.

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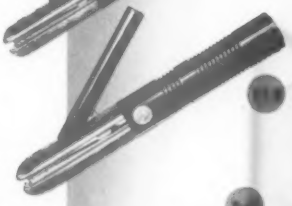
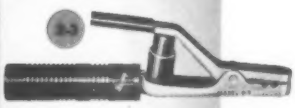
No. 9. *Copper in Stainless Steels*, by Helmut Thielsch, August 1951. Price: One dollar.





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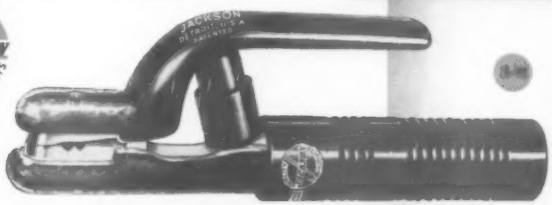

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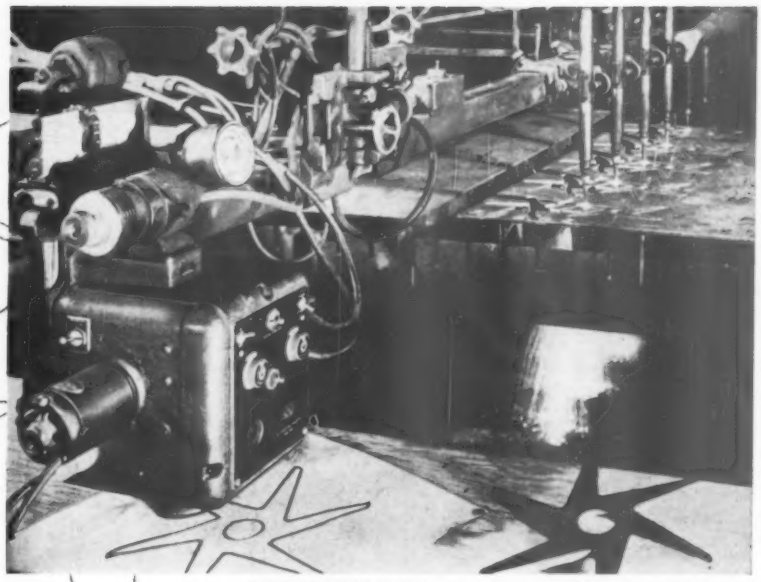

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